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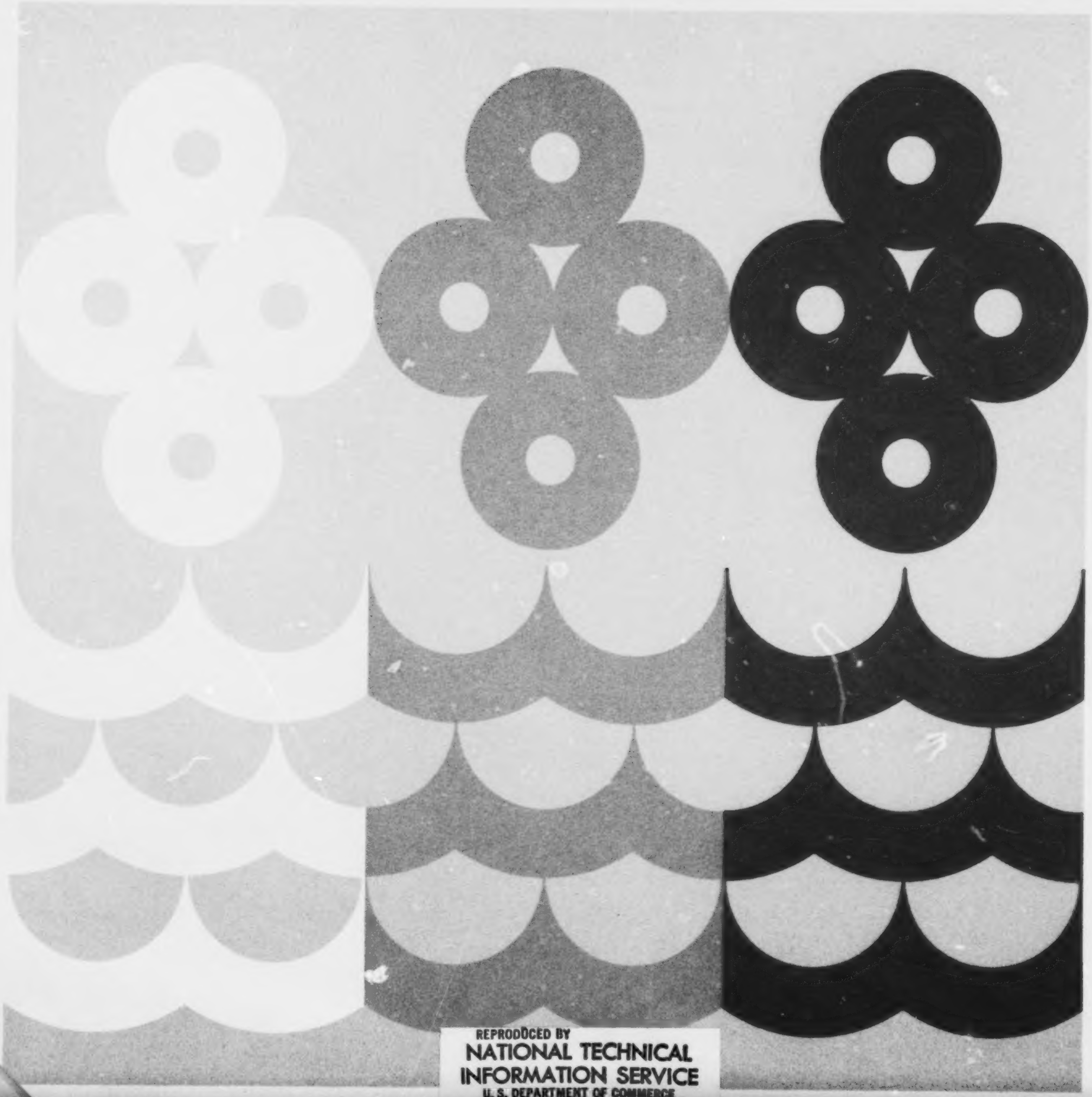

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16. ABSTRACT

The Workshop proceedings documented in this report represent a major effort to summarize historical practices and current art in the testing and evaluation of oxygen transfer devices used in the treatment of wastewater. These proceedings form the technical base from which the American Society of Civil Engineers' Subcommittee on Oxygen Transfer Standards is attempting to develop a tentative interim oxygen transfer standard on this research grant project.

The objectives of this Workshop were to bring together experts in the field of oxygen transfer to: (1) identify areas of agreement and disagreement in the evaluation of oxygen transfer devices and (2) identify research needs in the development of an effective consensus standard for oxygen transfer devices.

The first day-and-a-half were devoted to the presentation and discussion of topics related to the testing and evaluation of oxygen tranfer devices. The entire Workshop roster was then divided into five working group sessions, whereby intensive discussion took place in efforts to arrive at consensus opinions on selected topics. Areas of agreement and disagreement were delineated. The findings of each working group were presented to the reassembled total group on the final day of the Workshop.

The papers presented in the opening day-and-a-half general session and the summary reports of the working groups comprise the major portion of these Workshop proceedings.

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Edited by
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Foreword

The Environmental Protection Agency was created because of increasing public and government concern about the dangers of pollution to the health and welfare of the American people. Noxious air, foul water, and spoiled land are tragic testimony to the deterioration of our natural environment. The complexity of that environment and the interplay between its components require a concentrated and integrated attack on the problem.

Research and development is that necessary first step in problem solution and it involves defining the problem, measuring its impact, and searching for solutions. The Municipal Environmental Research Laboratory develops new and improved technology and systems for the prevention, treatment, and management of wastewater and solid and hazardous waste pollutant discharges from municipal and community sources, for the preservation and treatment of public drinking water supplies, and to minimize the adverse economic, social, health, and aesthetic effects of pollution. This publication is one of the products of that research; a most vital communications link between the researcher and the user community.

The **Workshop** proceedings documented herein represent a major effort to summarize historical practices and current art in the testing and evaluation of oxygen transfer devices used in the treatment of wastewater. These proceedings form the technical base from which the American Society of Civil Engineers' Subcommittee on Oxygen Transfer Standards is attempting to develop a tentative interim oxygen transfer standard on this research grant project. The activities of the Subcommittee offer an excellent forum for examining the merits and fallacies of existing and proposed aeration equipment testing and evaluation methodology as it struggles to define potential consensus positions in this controversial field.

Francis T. Mayo, Director
Municipal Environmental Research
Laboratory

Preface

As this country continues to search for more effective ways to conserve energy, greater emphasis is being placed upon the development of energy efficient processes in pollution control technology. The evaluation of the efficiency of oxygen transfer devices and systems will assume greater importance as engineers seek less energy intensive equipment. In spite of the considerable effort devoted to oxygen transfer technology, it is evident that unanimity of opinion has not been achieved in the development of a standard procedure for the evaluation of oxygen transfer devices. The areas of disagreement lie not only in the details of conducting oxygen transfer tests, but also in the methods of data evaluation.

Presently, manufacturers rely on clean water shop tests (i.e., performance tests conducted in a test tank at the manufacturing plant) for describing the oxygen transfer capability of aeration equipment. These capabilities are normally expressed as standard oxygen transfer rates in clean water at zero dissolved oxygen (DO) and 20°C. It has been shown that subtle differences in the method of data interpretation alone can produce differences of as much as 10% in the clean water standard oxygen transfer rate. Moreover, this uncertainty is further magnified when translating these clean water test tank transfer rates to actual plant conditions. Because of differences in wastewater characteristics, tank geometry, temperature, etc., uncertainties of 50% or more may be introduced.

This uncertainty has had a significant impact on the Nation's wastewater treatment plant construction program. Faced with this problem, many consulting engineers have written requirements for field performance testing into their specifications for aeration equipment. Contractors and manufacturers recognize this liability in preparing bids and will include larger factors of safety; thus, many aeration systems will be adequate but overdesigned. Some systems, however, may still prove to be inadequate.

There is little question that a consensus standard is needed for oxygen transfer devices. Although several standards are in existance, these standards are concerned primarily with the methodology of experimental measurement and do not deal adequately with the interpretation and application of data to engineering design. Moreover, there is no general agreement among engineers and manufacturers as to which standard to use. As a result, a wide variety of techniques are employed, resulting in substantial variations in results for the same device in clean water tests. Even larger variations will be evident in translating these results to full-scale design. Only when a standard is developed through consensus agreement among experts in the field will a better degree of uniformity, accuracy, and economy result. Even then, continued updating of the standard will be required.

In an effort to obtain a consensus standard for the evaluation of aeration devices, the American Society of Civil Engineers (ASCE) has established a Subcommittee on Oxygen Transfer Standards under the Committee on Environmental Standards (Technical Council on Codes and Standards). The procedures which the Subcommittee has set as its program to achieve a proposed interim standard include:

1. Review and critically evaluate the state of the art.
2. Evaluate and critically review existing standards; identify critical areas of disagreement and uncertainty.
3. Conduct a workshop on oxygen transfer.
4. Develop documentation for recommendations for an interim standard and recommend verification methodology.

Upon completion of the above tasks, the outputs will be integrated into a proposed interim standard (or, if thought appropriate, a recommended procedural manual). The Subcommittee will subsequently solicit from the field suggestions and comments on the proposed interim standard, determine verification methodology, field test the standard to verify its applicability contingent on the availability of resources, and revise and submit the proposed interim standard to ASCE for processing using the Society's voluntary consensus standards procedures.

As a part of the Subcommittee's objectives, then, this Workshop was developed. This is the first in a number of steps which will be taken in an effort to arrive at a consensus standard which will be acceptable to the manufacturers, the users, and the designers.

William C. Boyle, Chairman
Subcommittee on Oxygen Transfer Standards
American Society of Civil Engineers

Abstract

The objectives of this Workshop were to bring together experts in the field of oxygen transfer to: (1) identify areas of agreement and disagreement in the evaluation of oxygen transfer devices and (2) identify research needs in the development of an effective consensus standard for oxygen transfer devices.

The first day-and-a half were devoted to the presentation and discussion of topics related to the testing and evaluation of oxygen transfer devices. The entire Workshop roster was then divided into five working group sessions, whereby intensive discussions took place in efforts to arrive at consensus opinions on selected topics. Areas of agreement and disagreement were delineated. The findings of each working group were presented to the reassembled total group on the final day of the Workshop.

The papers presented in the opening day-and-a half general session and the summary reports of the working groups comprise the major portion of the Workshop proceedings.

These proceedings were submitted in partial fulfillment of Grant No. R805868 by the American Society of Civil Engineers under the sponsorship of the U.S. Environmental Protection Agency.

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Symbols and Nomenclature

A list of the more commonly used symbols and nomenclature are given below. An effort has been made to standardize most of the symbols in this proceedings using the listing below:

D = impeller diameter (L)
d_b = bubble diameter (L)
d_o = orifice diameter (L)
g = acceleration of gravity (L/t^2)
M = weight of water (m)
n = impeller speed (L/t)
p = pressure (f/L^2)
p_a = ambient atmospheric pressure (f/L^2)
p_{O_2} = partial pressure of oxygen (f/L^2)
P = power (fL/t)
Q = volumetric flow rate (L^3/t)
S = concentration of substrate (BOD, COD, etc.) (m/L^3)
V = volume (L^3)
X = concentration of active biomass (volatile suspended solids unless stated otherwise) (m/L^3)
ρ = mass density (m/L^3)
γ = weight density (f/L^3)

Mass Transfer Relationship, based on the overall liquid film coefficient

$$W = K_L a \, (C^* - C)$$

W = transfer rate per unit volume in clean water (m/L^3t)
W = dC/dt (for non-steady state non-flow conditions) (m/L^3t)
W_f = transfer rate per unit volume in dirty water field condition (m/L^3t)
K_L = overall liquid phase mass transfer coefficient, based on the liquid film (clean water) (L/t)
a = interfacial surface area per unit volume (L^{-1})
$K_L a$ = volumetric mass transfer coefficient, given conditions of geometry, mixing, temperature, etc, based on the liquid film (clean water) (t^{-1})
$(K_L a)_f$ = volumetric mass transfer coefficient under dirty water field conditions of geometry, mixing, temperature (t^{-1})
C = DO concentration in the liquid phase (m/L^3)
C^* = DO saturation (equilibrium) concentration in the liquid phase (m/L^3)
C^*_y = DO saturation concentration in clean water corresponding to a given partial pressure and temperature (m/L^3)
C^*_f = average DO saturation concentration in a dirty field application corresponding to an average gas phase partial pressure and temperature attained in the field application; defined so that $W_f = (K_L a)_f (C^*_f - C)$

C_s^* = DO saturation concentration corresponding to air at ambient atmospheric pressure and 100% relative humidity at a given temperature (surface saturation value) (m/L^3)

C_∞^* = DO saturation concentration attained at an infinite time in a non-steady state clean water test at a given temperature (m/L^3)

Other Symbols

α = ratio of K_La in dirty water application to K_La in clean water at equivalent conditions of temperature, geometry, mixing, etc. (dimensionless)

β = ratio of C^* in dirty water to C^* in clean water at equivalent conditions of temperature and partial pressure (dimensionless)

θ = temperature adjustment factor defined so that

$$\frac{(K_La)\text{ at temp. }T_1}{(K_La)\text{ at temp. }T_2} = \theta^{(T_1-T_2)}$$

for equivalent conditions of geometry, mixing, partial pressure, water quality (dimensionless)

OTE = oxygen transfer efficiency; mass rate of oxygen transferred divided by mass rate of oxygen supplied (dimensionless). Related to transfer and gas rates by:

$OTE = WV/\rho_a Q_a$

OTR = oxygen transfer rate; mass rate of oxygen transferred (m/t)

$OTR = WV$

SOTR = standardized OTR in clean water at stated conditions of temperature, dissolved oxygen, mixing, geometry, etc. Usually taken at 20°C and zero DO (m/t)

N = aeration efficiency; oxygen transfer rate divided by the power input (mt/tfL), commonly expressed as lb/hp·hr

$N = OTR/P$

N_o = standard aeration efficiency; aeration efficiency in clean water at standardized conditions, commonly taken at 20°C and zero DO (mt/tfL)

$N_o = SOTR/P$

R = volumetric oxygen uptake rate due to biological respiration (m/L^3t)

R_s = oxygen uptake rate per unit biomass due to biological respiration (m/mt)

$R_s = R/X$

Dimensions

m = mass   L = length   t = time   f = force   T = temperature (in °C unless otherwise indicated)

Acknowledgements

The time and effort of the American Society of Civil Engineers' Subcommittee on Oxygen Transfer Standards in planning and organizing this Workshop are gratefully acknowledged. The contributions of the Workshop participants and authors during the three days of meetings at Asilomar represent an excellent starting point toward the development of consensus procedures for the evaluation of oxygen transfer devices.

The cooperation of Mr. Robert B. Morgan of the American Society of Civil Engineers in arranging for the Workshop site and looking after numerous logistics details before, during, and after the Workshop was invaluable to the success of this conference.

Section I. A Perspective on Oxygen Transfer

1

Oxygen Transfer: A Historical Perspective – The Need for a Standard

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The development of an aeration standard has an interesting history. Prior to the mid 1950's, aeration consisted primarily of diffused air systems with specifications being based on ft^3 of air/gal of sewage treated. These specifications were based on experience.

Research was conducted during this period on the mechanism of oxygen transfer and aeration which led to the use of oxygen transfer coefficients (K_La) and transfer capacity in wastewaters (α). Other aeration devices, primarily mechanical surface aerators, came into vogue in the late 1950's and early 1960's. In order to differentiate aeration capacity and performance for various devices, test procedures were devised and adapted in one form or another by the various vendors of aeration devices. While all of these procedures involved deaeration with cobalt catalyzed sodium sulfite in water, the cobalt concentration varied and the method of calculation of oxygen saturation under the test conditions varied. During this period, the writer was involved in aerator testing for Wells Products, Walker Process and Mixing Equipment Co. More recently, several significant facts have emerged, namely (a) the cobalt concentration has a marked effect on the transfer rate calculated, (b) residual sodium sulfite may have an effect, (c) the oxygen saturation value used in the calculation will influence the transfer rate and (d) the temperature coefficient employed will influence scale-up to other operating conditions. Considerable controversy exists on the determination of these factors. There are other far more significant factors which will affect successful aeration performance. These are:

1. The definition of α under full-scale operation.
2. The effect of oxygen uptake rate on oxygen transfer.
3. The effect of basin geometry on aerator performance.
4. The effect of surface active agents on aerator performance, particularly mixing in the basin.

First, directing our attention to standard clean water transfer testing and specifications, it would appear that, based on the mass of information now available, agreement can be reached on cobalt catalyst concentration, maximum sodium sufite level, a calculation procedure for oxygen saturation and a temperature coefficient. Even if the values selected do not approach theoretical perfection, all vendors and all aeration devices will be based on a common standard for purposes of performance comparison.

In my opinion, it is far more important to define the aerator performance under field operating conditions. Concerning α, the accepted test is to compute K_La in the laboratory for water and the wastewater in question and calculate α as the ratio of K_La in wastewater/K_La in water. When mechanical aerators are used, α is related to the speed of the aerator. For an air diffuser, air flow is related to α. The difficulty is scaling this value to field conditions. The writer recalls data developed in Great Britain some years ago in which α varied from 0.9 to 1.4 at the same detergent concentration, in basins of different geometry, using surface aeratiors. Investigations are needed to establish a relationship between laboratory and field performance, if any, and to define procedures for determining α both in the field and in the laboratory.

There is evidence that aerator transfer rate is affected by the oxygen uptake rate. Albertson and Digregorio (1) showed an increasing oxygen transfer rate with increasing oxygen uptake rate. The writer was involved in an evaluation of the performance of low speed surface aerators treating a chemical wastewater. Data were collected over a one year period during which the organic loading progressively increased, resulting in an increase in oxygen uptake rate. Using the accepted steady state test, the oxygenation efficiency (lb O_2/hp·hr) increased linearly with oxygen uptake rate to a value of 6.4 lb O_2/hp·hr. The question is: Is the test valid or is there a significant effect of oxygen uptake rate on oxygen transfer rate?

Kalbskopf (2), von der Emde (3) and others have identified significant effects of the presence of surface active agents on mixing patterns in aeration basins. This in turn, no doubt, influences the oxygen transfer rate. Further definition of this phenomenon is needed.

To illustrate this point, studies were conducted by the writer on an aeration basin employing jet aerators.

Water tests had been conducted in this basin prior to the activated sludge studies. Tests were conducted to define α in the laboratory. Non-steady state tests were conducted on the basin contents and oxygenation capacity was evaluated from the COD removal in the basin. Steady state tests were also conducted. The results of these tests are summarized below.

The differences between values obtained is significant. It should be noted that the oxygen uptake rate for the steady state test tends to read low since the sample withdrawn from the aeration basin is without food during the course of the test.

Results of Basin Tests for Oxygenation Capacity Basis

20°C, 1 mg/l DO

Steady State	5220 lb O_2/day
Clean Water Corrected for α and DO	9300 lb O_2/day
COD Removal	6300 lb O_2/day

References

1. Albertson, O.E. and D. DiGregorio, "Biologically Mediated Inconsistencies in Aeration Equipment Performance", Journal Water Pollution Control Federation, 47, 976, 1975.
2. Kalbskopf, K.H., "Flow Velocities in Aeration Tanks with Mechanical Aerators", Water Research, 6, 413, 1972.
3. von der Emde, W. "Aeration Development in Europe." Advances in Water Quality Improvement, Vol. 1, edited by Gloyna and Eckenfelder, University of Texas Press, 1968.

Discussion

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Studies at the Water Research Centre have shown that changes in the concentration of suspended solids in the mixed liquor of an activated-sludge plant aerated by a fine-bubble diffused-air system had a slight effect on the rate of oxygen transfer. The oxygen transfer coefficient decreased by about 20% when the concentration of suspended solids was increased from 1400 to 6600 mg/l.

An Aeration Equipment Manufacturer's View of an Oxygen Transfer Testing Standard

2

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Introduction

A manufacturer must become involved with the formulation of an oxygen transfer test because it affects his goals. The present variety of "official standards" leaves a manufacturer in a quandry as to which standard should be used. The problem is further complicated by the inability of the trade to agree on units of measurement and their definitions. The standard that this Workshop arrives at must be complete and universally accepted in order that our efforts are not in vain.

Manufacturer's Goal

When one bypasses all of the rhetoric that is currently being put forth concerning a manufacturer's commitment to God, country, and society, the baseline is that a manufacturer's prime objective is to make a profit. Realistically, all else is subservient to this goal and any additional goals can only be pursued because that profit is made. As discussed below, the absence or presence of an oxygen transfer test standard falls way down the list of factors that influence a manufacturer's profit. But it is a factor that must be dealt with.

To reach this goal a manufacturer must consider a large number of factors including those listed in Table 1. While the list is not all-inclusive by any means, it should be obvious that if several of these factors are negative, a manufacturer will have a poor profit margin. This paper is restricted to the effect of oxygen transfer testing, what it should encompass, and how testing affects a manufacturer.

Table 1. Factors Affecting an Aeration Manufacturer's Goal

General economy
Salesman's ability
Proprietary products
Government approval
Economical manufacturing
Labor unions
Locations
Transportation
Weather
Acceptance of equipment
Believable design
Reliable construction
Competing processes
Competing equipment
Standard performance ratings

Manufacturer's Requirements for an Oxygen Transfer Test

In the best interest of each manufacturer is the test that proves his piece of equipment to be better than the competitor's. A wide spectrum of tests, of varying degrees of reliability, have been developed to serve this purpose. Some of these tests are analagous to government issued MPG ratings which show that a VW gets better gas mileage than a Porsche, while many others are similar to a manufacturer's self-proclaimed superiority of "whiter-whites" in your shirts. The oxygen transfer tests presently used fall closer to the latter image than the former.

At least the government issued MPG rating has some believability. Many oxygen transfer tests, like the whiter whites, generally are not acceptable to very many people since there are too many unknown variables. An oxygen test should be acceptable to all parties participating in an evaluation. Many times this is not so. Furthermore, the test should be believable. All of us unquestioningly accept the electric company's reading and report of our electrical usage. On the other hand, many consultants will not accept "MY" test method or in fact any other method except their own pet procedure.

The test manufacturers prefer it should be easy to understand, simple to perfrom, and low in cost. It should be both accurate and reproducible. The value of the answer must be known. Can it be used directly or is there a random scale-up factor? The test method and the results must relate to the end use design. It is not reasonable to test in a 55-gal barrel and scale-up to a 10 million gal basin. Equally important is a direct relationship between variations in test results and equipment design or manufacturing procedures. If variations in equipment have less effect than variations in the results obtained between test methods, then the manufacturer does not have any need for a test. The unknowns and the buried assumptions incorporated in present test procedures make it difficult to determine whether the "right" answer has been obtained or not. Therefore, a manufacturer can in all honesty proclaim the method he uses is the only one that correctly evaluates his equipment and design.

Requirements for an Oxygen Transfer Test Standard

A standard that would be acceptable to a manufacturer may not be acceptable to a scientist, but it should be. Therefore, we should develop the most thorough standard possible and let the manufacturers adjust to it. What is needed for oxygen standard is one having the status of an ASTM test, required by EPA for design, and listed in "Standard Methods". Another test method, standard or not, will not gain any greater acceptance unless there is a quasi-legal requirement for its use. A major problem for a manufacturer today is which method to use. Table 2 lists over two dozen procedures that are currently in use. Which one should a manufacturer use? Which should a consultant design by? By which one does an owner rate his system?

Table 2. Oxygen Transfer Procedures

- **Mine**
- WPCF/ASCE
- PEMA
- TAPPI
- ASCE
- WWEMA
- ASTM
- EPA
- Kenics
- Mixco
- R. B. Carter Company
- Union Carbide
- Penberthy
- Sanitaire
- RAMCO
- Dupont
- CH_2M Hill
- Black & Veatch
- Hazen & Sawyer
- Shell
- Kalinske
- Downing
- Kayser
- Non-Steady State Biological
- Steady State Biological
- Steady State Sulfite
- Non-Steady State Sulfite

A number of philosophical problems must be considered in arriving at a new standard. Table 3 lists several philosophical problems already mentioned and many others that must be evaluated by this Workshop. However, before these questions are considered in detail, the specific conditions of a test standard must be agreed upon. There is little need to discuss the broader philosophy of a standard when as a trade group we can't even agree upon the definitions. The instructions to speakers at this conference had to include a standard list of terms for writers to use. Even more basic than that is an agreement as to what an individual term means or measures. For example, Table 4 lists 13 conditions specifying what a standard cubic foot is. The term standard is used in the writer's instructions without being defined. This is a potential source of error of over 18% before the Workshop even starts. Even this table is not sufficient to define what a standard cubic foot of air is. Since air is a mixture, the constituents and their concentrations must also be defined. Ideally, our standard cubic foot should be defined to the point of ridiculousness as follows: that amount of air contained in a cubic foot of space as measured by a NBS certified wet test meter at 68°F, 14.6960 psia, 36% RH, containing 78.08%v N_2, 20.95%v O_2, 0.93%v Argon, 0.034% v CO_2, and 0.01%v trace elements with an average molecular weight of 29.100. From here on one can make whatever simplifications are felt necessary, but at least we all start from a common reference point.

Table 3. Philosophical Points to be Considered in Arriving at a New Oxygen Transfer Test Standard

1. Universally accepted.
2. Everyone required to use, either legally or by peer pressure.
3. Will all equipment maintain the same relative differences as they now have?
4. Will it correlate with past tests and data?
5. Capability of all manufacturers to properly test and retest and understand.
6. Capability of all consultants and owners to properly test and retest and understand.
7. Narrowness of input to standard. Lack of input from other diciplines. (Chemical engineering, pharmaceuticals, petro-chem, etc.).
8. Applicable to all aeration equipment. What about rotating biological contractors?
9. Simple to run.
10. Cheap to perform.
11. Accuracy and precision.
12. Reproducibility.
13. Is this change for changes sake?

The standard arrived at by this Workshop must be this complete and all encompassing. It should apply to all biological treatment devices. There may well be variations in procedure, depending upon the generic type, but all types as listed in Table 5 should be covered. The various types of oxygen tests listed in Table 6 should be included within the overall test standard. In addition, the more specific aspects of an oxygen standard listed in Table 7 will have to be specified. The final standard must address itself to these and other aspects in order to be of value to manufacturers, consultants, and owners.

Summary

The final published standard should be of such a form that it can be expanded by sections to cover all methods

Table 4. Standard Conditions for Gas Measurements

Source	Pressure	Temperature	Humidity	ft^{3*}
Chemist	1.0 ATM	0°C	0%	1.0000
Engineer	14.7 psia	32°F	?	0.997
Federal Power Commission	14.73 psia	60°F	?	1.0545
ASCE (Gas book)	14.73 psia	60°F	?	1.0545
European	1.033 kg/cm^2	0°C	?	0.9998
European	1.033 kg/cm^2	15°C	?	1.0547
European	1.000 kg/cm^2	0°C	?	0.9678
European	1.000 kg/cm^2	15°C	?	1.0210
European	76 cm Hg	10°C	?	1.0366
HVAC	14.7 psia	68°F	?	1.0735
HVAC	14.7 psia	70°F	?	1.0776
Hoffman Blowers	14.70 psia	68°F	36%	1.1725
ASCE-O_2 Symbol Table	1 ATM	20°C	100%	1.1852

1 ATM = 14.6960 psia = 1.03323 kg/cm^2 = 76.0 cm Hg.

* Assumed % RH zero for all unknown cases.

Table 5. Aeration Equipment

- Surface impeller, high and low speed
- Submerged impeller
- Submerged turbine, upflow and downflow
- Fine bubble diffusers
- Coarse bubble diffusers
- Static tubes
- Eductor/ejector
- Inline mixers, static and dynamic
- Rotating biological contactors
- Trickling filters
- Cooling tower style trickling filters

and equipment. In the limited time available to us, we should restrict our efforts to producing a comprehensive method for a pure water shop test. Once we have the most commonly used method in printed form, the expansion of this standard to cover clean water field test and wastewater testing will be considerably easier. There will always be a need for clean versus dirty water testing, and shop versus field testing. Not every manufacturer has access to a properly designed tank for shop test. Nor is every field test compatible with clean water methods. For example, how does one evenly and quickly distribute sulfite over a basin several acres in size? The pure water shop test should be established as the base procedure with all other variations referenced back to it.

In order to promote usage and general acceptance, ASCE must publicize the method, not only in our internal magazine *Civil Engineering*, but also in other trade publications. The EPA needs to state that this standard is the only one they approve for use in aeration equipment evaluations and that it must be used in all federally funded projects. Finally, the remaining 10,000 plus consultants who couldn't attend this Workshop must be sold the advantages of specifying this standard.

Manufacturers can help the overall process by accepting and using the standard to evaluate their equipment. The most difficult problem for a manufacturer, and indeed for many others, will be what does one do with the large quantity of results previously measured and calculated. For it is quite probable that our final standard will not be identical to what any of us are presently using. For example, the Workshop may conclude that siphon lines or pumped samples are no longer acceptable and that *in situ* measurements must be made. Or if we conclude that the "Analysis of Variance" method of data analysis is to be the base procedure, what do those who have used the "Log Deficit" method do? Not only do we have to establish a general standard, but we must specify one of the test conditions listed in Table 6 and define each of the individual aspects listed in Table 7. Furthermore, we will have to establish correlations between our prime or basic procedures and the other closely related variations.

It is soon evident that what on the surface appears to be a simple test is going to be very difficult to accomplish.

Table 6. Test Conditions

- Shop
- Field
- Clean (how clean)
- Dirty (how dirty)
- Wastewater
- Sulfite methods
- Steady state methods
- Non-steady state methods

In conclusion, I would like to state that I anticipate that when we are all done the "new" answer to a transfer test will be within the accuracy of our present methods. In other words, I see no net effect to a manufacturer of a standard other than that there will now be one generally accepted test procedure. The existence of one standard will reduce the bickering and finger-pointing that now goes on. This will make life easier for all of us.

Table 7. Aspects of Oxygen Testing

- Sampling
- Water analysis
- Data analysis
- Chemicals, purity and type
- Power input
- Tank size
- Test apparatus
- Measuring apparatus
- Sulfite distribution
- Aerators per unit volume
- Aerators per unit surface area
- Spacing
- Relationship of test to field design

The Development of an Oxygen Transfer Standard

3

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To the consulting engineer the selection of aeration equipment involves the development of a total system capable of serving a variety of needs within the waste treatment plant. The most efficient and economical transfer of DO is only one of several major criteria that must be satisfied.

The aerator is also the only source of mixing within the system and must provide sufficient velocity within the tanks to effectively provide contact between the waste and the microorganisms. This mixing must be provided in a manner compatible with the sheer tolerance of the organisms and other biotic constraints. If these criteria are not satisfactorily accomplished, the process, even in the presence of sufficient DO, will not serve its design purpose.

Oxygen transfer mechanisms also significantly influence the rate of heat loss within the system. In general, compressed air type systems add heat to the liquid while surface aeration systems result in heat loss. In large volume systems the influence of oxygen transfer devices on heat balance can have a major impact on the treatment efficiency of the system. Other impacts associated with aeration include effects on pH through stripping and ambient noise levels.

The choice of aeration equipment influences the basic geometry of the plant and therefore the capital costs. This phenomenon is of course interrelated with the flexibility of the system and its ability to respond to variability in the waste flow and the ease with which future expansions can be accomplished. For example, although a large single aerator in a square tank might be the most efficient oxygen transfer design, a combination of three or four smaller aerators in a group of tanks in a series or parallel may very well present the better overall design.

From the discussion above it can be seen that oxygen transfer alone cannot be considered as the sole criterion when developing an oxygen transfer standard. Rather, any system must be viewed in terms of its overall compatibility with the job to be accomplished. The standard as evolved must be sufficiently flexible to allow the consulting engineer the wide range of latitude necessary for the optimization of oxygen transfer within his particular design context.

Discussion

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Ed Barnhart has stated in a nutshell what the significance of an oxygenation standard is in the overall consideration for the design of a waste treatment plant. To place too much emphasis on a standard which, at best, is going to be controversial for years to come can create a "penny wise, pound foolish" situation in the waste treatment profession. The aeration 'tail' can be found wagging the treatment plant 'dog'.

This does not imply that a standard test is not desirable. Certainly it would be a relief to know that every investigator would use the same temperature correction factor (theta value) to relate to a 20°C standard condition. If a single value for theta cannot be agreed upon, several values being supported by credible scientific investigation, the logical question is whether the factor is, in fact, a *temperature* one. Certainly, it would be *a* relief to know that there is a reference water which can be universally reproduced for use anywhere in the world to provide reproducible standard results anywhere in the world.

Assuming that, in time, all of these factors can be standardized one by one, we are still left with the uncertainty of the alphas, betas, geometries, etc., that exist in the real world of waste treatment. The uncertainty of these factors assuredly has a greater influence on the ultimate success of the aeration system than does the uncertainty of standard test factors.

It would seem strong consideration must be given to separating the research efforts into two parallel studies, one to develop the *standard* tests and the other to conduct full-scale field tests to *relate back* to the standard. The first can be the basic research progression from laboratory to pilot plant to full scale. At the point of full scale, to be completely objective, it would be necessary to investigate every type of aeration device in the minimum sized structure that truly represents an actual treatment plant of the optimum geometry. The second would involve planned field studies in full-scale plants with the actual wastes to be treated, following studies first utilizing the proven reference water. Only after a number of these studies with each style of aeration device could a relationship, or lack thereof, be developed to predict actual treatment from mere standard tests.

The greatest potential pitfall ahead is to unwittingly bias studies toward or away from styles of aeration equipment for lack of objectivity or failing to, as noted by Barnhart, consider all aspects of a *complete* treatment system.

Philosophy of and Perspectives on a Standard by an Owner Representative

4

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While I am an employee of the Milwaukee Sewerage District and am responsible for the operation and maintenance of one of the oldest activated sludge plants in the world, I am here to speak for all owners, be they public or private.

It makes little difference to the consumer whether he pays for the installation of improper equipment at the tax counter or the store counter. In the ultimate end, it is always the consumer who pays. Therefore, I feel I represent not only the User as indicated on the program, but the total public who pay the ultimate cost.

A performance standard for oxygen transfer devices will prevent the installation of equipment more efficient and more expensive than is needed to do the job. It should also allow selection of equipment which will do the needed job and not require expensive replacement because it fails to do the job.

The User's needs will vary from treating a dilute settled municipal wastewater to treating a high strength industrial waste.

The User is entitled to a test procedure which:

1. Will be easily understood by the Manufacturer, the Consulting Engineer, the Installing Contractor-Bidder, and the User
2. Will give representative results in a test situation before installation in the treatment plant
3. Can be field tested in place prior to being placed on the production line.

I am in no position to tell you how to provide the above test procedures. I hope that this conference will develop them because they have been needed for a long, long time.

We see more stringent effluent standards being set at all regulatory levels. The installation and operation of better facilities will be required to meet these standards. Development of an oxygen transfer standard that can be used to predict performance under actual operating conditions will make attainment of these new standards more probable.

Standards in the American Society of Civil Engineers

5

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What is a voluntary consensus standard? This is a question which has been wrestled with for many years. In the sense of ASCE's activities it is a resource for voluntary use by designers, owners, politicians and regulatory agencies, to name only a few. As a voluntary consensus standard gains acceptance it may become legal and binding if it is adopted by regulatory agencies such as federal, state or local governments. It is then often referred to as a code. We should realize that ASCE is not in the code business but rather in the consensus standards business, although consensus standards provide a resource base for code development. Standards can become synonomous with and identical to portions of codes if a standard is a good resource, as codes are generally an assimilation of several standards.

Inconsistant terminology often creates confusion. Standards are sometimes referred to as codes and the preface "model" may also be used. Model is a very descriptive and appropriate modifier. Model building codes such as those of Building Officials and Code Administrators (BOCA), International Conference of Building Officials (ICBO) and Standard Building Code Congress (SBCC) have been in existence for many years. Perhaps the most advanced terminology is *model performance standards*. The performance concept encourages innovation and has an important ingredient, a provision for evaluating performance.

The voluntary consensus standard committee must have special characteristics:

Balance — The committee must be made up of those who are affected by the standard. Affected parties should be equally represented on the committee.

Consensus — A consensus must be reached with all negative opinion resolved. The consensus starts with the committee and reaches to the public. ASCE's audience for consensus is the enrolled members of the Technical Council on Codes and Standards (TCCS). The public consensus would come through the approval of ASCE's standards by the American National Standards Institute (ANSI) as American National Standards.

ASCE became an ANSI accredited standards making organization in December 1977, which represented a major accomplishment in the early stages of the Society's standards program. The administration of standards committees is centered at ASCE Headquarters under the general charge of the Managing Director of Publications and Technical Affairs and with the Technical Council on Codes and Standards Executive Committee. In addition to administrative responsibilities for the standards activities, Headquarters and TCCS are responsible for liaison to ANSI and other standards organizations (ASTM, ASME, ACI, etc.), and also for following legislation and other items that affect ASCE's standards activities. The technical focus lies within the standards committees with appropriate contributions from the Society's technical committees.

The need for a standard must be established. The extent of need may be determined through answers to questions such as:

Does a similar standard exist, i.e., will a new one be redundant?

Is the standard necessary for protection of life, safety or performance?

Will a standard unnecessarily interfere with the market place or contribute to it?

These items are, of course, not exhaustive. Once the need is established, the formulation of a standard must result from a carefully assimilated plan beginning with the first word and going through the standard's implementation. The responsibility assumed when a committee partakes of standards writing is immense. Responsibility has broad implications. Legal obligations fall within the jurisdiction of laws enforced by the Federal Trade Commission and the Department of Justice and relate to such items as the restraint of trade and boycott. Another legal responsibility is the liability for the standard's content. For example, does the standard, if deemed to be met, provide the safety or performance stated or intended? There is a responsibility to society in general which in turn is related to the justification of the need for a standard as previously discussed. A professional responsibility is assumed by the committee to ensure technical quality of a standard. Technical quality should be continually critiqued and not compromised.

Proper attention to evolution is very important for the implementation of a standard. Unfortunately, improper evolution, i.e., transformation from state-of-the-art to a standard, cannot be determined until it is too late. Large changes from current practice or accepted usage result in unused standards, and inadequate use of the state of the art results in early obsolescence. Some questions can be asked and, if answered correctly, can make up a set of guidelines that correlate to appropriate evolution.

What exists now?

What is the state-of-the-art?

What portion of the state-of-the-art is credible?

What will the audience accept?

It should be noted that the portion of the state-of-the-art that can be used is that which is the most credible and acceptable, remembering that experience has shown the state-of-the-art or technology often leads applications or standards by 5 to 10 years or more.

Proper identification of the audience for a standard may be deceptive. Standards can have broad or narrow audiences, and individuals or groups are addressed directly or indirectly. The direct audience should be the primary focus. Standards are used by the designer or industry to meet an obligation of public protection and quality. However, standards are also used by engineers, politicians, attorneys, and economists in the decision making process long before the standard is applied to the particular product, such as a building, water treatment facility or equipment.

Standards can become complex; however, unnecessary complication should be avoided. There are several tools of technology that have recently become practically available and applicable to the formulation of standards. Performance concepts provide and encourage innovation while still requiring that performance is met. Probabilistic methods allow the handling of random processes in a formal manner to estimate risk, means, variability and uncertainty. Sometimes probabilistic approaches can provide a method for simplifying to a deterministic format. Decision analysis is a useful tool for analyzing the logic or sequence of a standard and the presence of loops, voids and dead ends. It also can provide for the construction of a simple guide for the application of a standard. Commentary, guidelines or manuals of practice for standards are the necessary textbooks and roadmaps. Keeping in mind that, both nationally and internationally, the International System of Units (SI) will be in the forefront in the near future, alternative metric units should be provided in all standards.

Last but not least, no matter how good a standard is, it won't be used if potential users are not informed. Good standards have to be promulgated through advertising, education, discussion and application. This process is neither self-starting nor self-perpetuating. The Society as well as all those involved in a standard have an obligation to an end — the popular use of a voluntary consensus standard — which is the only measure of success.

Philosophy of and Perspectives on an Oxygen Transfer Standard—The EPA View

6

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Introduction

Because it is removed from the design selection process, the U.S. Environmental Protection Agency's (EPA) construction grants program is totally reliant on the comprehensiveness and accuracy of the historical treatment data, pilot studies, and testing procedures which form the bases for plant designs. The design engineer is also dependent on these data bases as well as his technical judgement in evaluating alternative design strategies and selecting the most cost-effective option for each client.

Greater attention has been given in recent years to process oriented research and demonstration studies than to the development of techniques for measuring equipment performance on a standardized basis. We have specific effluent quality criteria against which process performance is normally judged and in most cases well established analytical and monitoring procedures for determining compliance with those criteria. On the other hand, our bases for selecting equipment to implement a particular process design are fragmented, inadequate, and often unintentionally biased. It is virtually impossible to predict with any degree of confidence how some types of equipment will respond or perform in wastewaters or on wastewater sludges of differing characteristics.

Nowhere is this dilemma more apparent and potentionally cost wasteful than in the design and selection of oxygen transfer equipment. Each manufacturer has its own philosophy regarding the measurement, evaluation, and prediction of oxygen transfer. Two common complaints often voiced by engineers are the difficulties encountered in (1) correlating manufacturers' oxygenation claims with shop test or field test data and (2) projecting equipment performance in a respiring biological system once it is felt a representative clean water oxygen transfer capability has been established. The discrepancies between anticipated and actual performance are often sufficiently large to warrant substantial field modifications to the aeration equipment furnished. The costs of performing such modifications and the ill will generated offer vivid testimony of the need for improved oxygen transfer design procedures.

General Thoughts on a Standard

The development of a consensus standard for testing and evaluating oxygen transfer devices in clean water and wastewater cannot be circumvented if we are to acquire uniformity and reliability in the art of specifying aeration equipment. EPA fully endorses the objectives of this Workshop and the American Society of Civil Engineers' (ASCE) Subcommittee on Oxygen Transfer Standards (formed under the Society's Technical Council on Codes and Standards) who devised the Workshop program.

EPA is under no illusions that the development of a consensus standard(s) will be an easy task or that it can be accomplished without a great deal of anguishing over and wrestling with the technical issues involved. That the attempt will be made through voluntary participation by a consortium of experts in the field such as embodied in the above designated ASCE Subcommittee rather than under EPA direction is highly preferred. The stakes have become too high not to begin moving toward a unified approach to testing and evaluation. It should also be appreciated that after 5 yr of staff development and program administration, the construction grants program is now in a position to take a more active role in assessing design bases and standards. Thus, by initiating its own long-range plan to develop a consensus standard, the oxygen transfer field is forestalling potential government intervention in an area best left to the day-by-day practitioners.

Benefits of a Standard

In addition to greatly reducing the costs involved in correcting aeration equipment deficiencies and making field modifications when in-process performance does not measure up to specified performance, other benefits to the Nation which can be expected to evolve from utilization and wide-spread acceptance of standardized, verified-accurate testing and evaluation procedures are:

1. Stimulation of greater deployment of more efficient oxygen transfer systems with concommitant savings in energy
2. Reduction of bio-reactor volume requirements and construction costs arising from increased usage of effective high-rate activated sludge systems, again due to rising confidence in and greater willingness to specify higher-efficiency oxygen transfer equipment

3. Elimination of excessive over-design techniques now employed by some design firms to avoid unintended undersizing of oxygen transfer equipment.

Decreased Aeration Power Requirements

Due to the rapid escalation of the cost of energy in recent years, there exists for the first time in the history of our country a compelling economic incentive to move toward broader use of high efficiency oxygen transfer equipment in lieu of relatively maintenance free, low efficiency coarse bubble diffuser and sparger systems. Increased oxygen transfer capability is now available in several forms including a variety of fine bubble diffusers and aerators, medium-size bubble aeration devices, submerged turbines, surface aerators, and even improved coarse bubble equipment designs. Generally speaking, the smaller the bubble, the greater the oxygen transfer percentage achieved. Other factors such as mixing and bubble-contacting design, wastewater chemistry, and wastewater temperature also strongly impact the performance of aeration and oxygenation equipment in wastewater. In the final analysis, though, the size of the bubble defines the upper limit of oxygen transfer efficiency which with proper aeration design can be approached. In the United Kingdom and continental Europe, where expensive power has been a fact of life for several decades, fine bubble aeration systems have been receiving a high degree of acceptance and utility for many years.

For comparative purposes, claimed and/or commonly-accepted clean water oxygen transfer efficiencies and rates are summarized in Table 1 for several generic aeration devices. The information presented is for standard conditions of temperature and pressure and an initial DO concentration of 0 mg/l. It is quite evident that if the claimed oxygen transfer capabilities of fine bubble aeration systems in particular can be substantiated via repeated testing using valid, standardized test and evaluation methodology and if the maintenance requirements of these devices are not exorbitant, substantial savings in power are possible in contrast to the traditional, widely-used coarse bubble options.

Decreased Bio-Reactor Volume Requirements

The accelerated development and implementation of oxygen activated sludge systems have revealed that United States sanitary engineers and wastewater treatment technology users are ready and willing to employ efficient high-rate activated sludge designs. Our friends in Europe undoubtedly feel it is about time. The definition of an efficient high-rate activated sludge system as used here infers high mixed liquor suspended solids (MLSS) concentrations, low reactor detention times, high volumetric organic loadings, higher than traditional food-to-microorganism (F/M) ratios, and the production of a high quality secondary effluent. It should not be confused with the low detention time, low MLSS modified aeration system popular in the 60's, which yields only 60-70% BOD removal.

The key to efficient high-rate activated sludge treatment is operation with high biomass concentrations. Healthy, highly-concentrated, readily-settleable biomass can be effectively sustained only by efficient oxygen transfer and the absolute elimination of zones of DO deficiency. It may not be possible to develop high-rate air systems with nominal reactor detention times as low as those typically specified for municipal oxygen systems (1.5-2.5 hr based on Q) because of the disparity in the driving force of the two gases. However, if the claimed transfer efficiencies of the newer generation air devices shown in Table 1 can stand up to the repeated scrutiny of a consensus testing procedure, it appears realistic to anticipate that traditional coarse bubble air reactor nominal detention times of 5-7 hr can be reduced to 2.5-3.5 hr in many municipal treatment situations. Design parameter ranges typically selected for municipal oxygen and coarse bubble air activated sludge plants are compared in Table 2 with this writer's projection of a potentially feasible design and operating range for high-rate air systems employing high efficiency oxygen transfer equipment.

Table 1. Comparative Clean Water Oxygen Transfer Information for Air Aeration Systems at 15 ft (4.6 m) Submergence

System	Oxygen Transfer Efficiency (%)	Oxygen Transfer Rate (lb/wire hp·hr)‡
Fine bubble diffusers, total floor coverage	20-32*	4.0-6.5
Fine bubble diffusers, side wall installation	11-15*	2.2-3.0
Jet aerators (fine bubble)	22-27*	4.0-5.0
Static aerators (medium-size bubble)	12-14*	2.3-2.8
Mechanical surface aerators	--	2.5-3.5**
Coarse bubble diffusers, wide band pattern	6-8**	1.2-1.6
Coarse bubble diffusers, narrow band pattern	4-6**	0.8-1.2

‡ 1 lb/hp·hr = 0.61 kg/kW·hr

* Taken from manufacturers' company bulletins and technical reports where available.

** Commonly accepted ranges for historically used variations of these generic systems.

Table 2. Design Parameter Ranges for Municipal Activated Sludge Systems

Parameter	Oxygen (typical)	Air, Low O_2 Transfer Efficiency (typical)	Air, High O_2 Transfer Efficiency (projected)
Aeration detention time based on Q (hr)	1.5-2.5	5-7	2.5-3.5
MLSS (mg/l)	3500-6000	1500-2500	3000-4000
F/M loading (kg BOD/day · kg MLVSS)	0.5-0.8	0.25-0.5	0.35-0.7
Volumetric loading (lb BOD/day · 1000 ft^3)‡	100-200	30-60	75-150
Mixed liquor DO (mg/l)	4-8	0.5-2	2-3
Return sludge TSS (%)	1.5-2.5	0.75-1.5	1-2
Clarifier overflow rate (gpd/ft^2)*	450-650	500-750	450-650

‡ 1 lb/day/1000 ft^3 = 0.016 kg/day · m^3

* 1 gpd/ft^2 = 0.041 m^3/day · m^2

Aeration Upgrading Example

In 1975, the Wastewater Research Division of EPA's Cincinnati based Municipal Environmental Research Laboratory (MERL) was requested to perform a desk-top analysis of potential alternative techniques for upgrading a 190-mgd (8.3-m^3/sec) activated sludge plant on the East coast. The plant was designed and operated as a modified aeration system with 2.5 hr of aeration time based on plant influent flow. Plant effluent quality was poor, averaging 57 mg/l of BOD and 74 mg/l of suspended solids for the period of July 1974 through June 1975.

The major problem was diagnosed as an inadequate air supply and delivery system. A combination of three centrifugal and three positive displacement blowers provided a total air supply capacity of approximately 0.64 ft^3/gal (4.8 m^3/m^3) of influent flow. Computer analysis of past plant operating records indicated that the existing spiral-roll, coarse bubble diffuser system was achieving a process oxygen transfer efficiency of only about 2.65%. The mixed liquor was often devoid of any measurable DO and black in appearance with a noticeable hydrogen sulfide odor. High soluble effluent BOD's that varied from 25-50 mg/l were further evidence of oxygen deficiency and anomalous activated sludge performance.

Evaluation of potential upgrading alternatives revealed that by far the most cost-effective solution would be replacement of the existing coarse bubble diffusers with higher efficiency oxygen transfer devices. Two different types of higher efficiency aeration systems were studied, a fine bubble diffuser system and a medium-size bubble static aerator system, both designed for total basin-floor coverage.

Computer modeling of anticipated performance with an upgraded aeration system predicted that an effluent total BOD of 30 mg/l or less would be achieved provided effluent suspended solids could be lowered through better bioflocculation to 45 mg/l. If effluent suspended solids remained at the pre-upgrading 75 mg/l level due to the prevailing overloaded secondary clarifier condition, effluent total BOD's in the range of 35-40 mg/l would be expected. It was further predicted that effluent soluble BOD's would be decreased to 6-12 mg/l depending on wastewater temperature. Based on historical organic loading patterns, it was estimated that the static aerator system would be unable to meet a target mixed liquor DO level of 2 mg/l only 10-15 days/yr and that the fine bubble diffuser system would always meet the target level.

Capital costs were estimated for both aeration systems and projected to July 1977 dollars. Construction in both cases would be limited to removing and replacing the existing diffusers, lateral piping, and downcomers within the 20 existing basins (basin dimensions are 410 ft long x 22.5 ft wide x 15 ft SWD = 125 m x 6.9 m x 4.6 m). Modifications to the existing blower complex and the header piping system delivering air to the new downcomers would not be required. Electrostatic precipitator/agglomerators and bag collectors would have to be added to the plant's existing air prefilters for the fine bubble diffuser option but not for the static aerators. Aeration system cost estimates were solicited from representative manufacturers of the two aeration system types. Total capital costs, including allowances for engineering charges; legal, fiscal, and administrative fees; and interest during construction, were estimated at $1.35 million for the fine bubble diffuser system and $0.85 million for the static aerator system.

Operating and maintenance costs were also estimated. For the fine bubble diffuser system, diffuser replacement once every 10-yr was factored into the estimate at a per occurrence cost of $110,000 in the event long-term clogging would be experienced. An annual cost of $1500 was also included for maintenance on the expanded air filtration set-up required by the fine bubble diffuser system. Estimated power costs were calculated based on a rate of 3¢/kW·hr using projected oxygen transfer efficiencies provided by the respective manufacturers.

Total annual cost, including amortization and the above described diffuser replacement and air filtration maintenance costs, for the two upgraded aeration systems are compared in Table 3 with the existing coarse bubble diffuser system. Because of the large potential reduction in power costs, the information in Table 3 suggests that the subject plant could absorb the debt retirement charge of the new equipment and still realize substantial savings in total annual cost with either system over that now expended for power alone with its coarse bubble diffusers. At the same time, the plant would be benefiting from much improved biological performance.

The above example illustrates the type of thinking and design approach favored by EPA in dealing with the energy-sensitive activated sludge process. The establishment of a standardized test protocol would lend credibility to the claims of high oxygen transfer efficiencies reported for some aeration devices and would instill increased user market confidence in these devices.

EPA Research Program in Oxygen Transfer

Within the constraints of its budget resources, the Wastewater Research Division of MERL is supporting research studies directed toward improved aeration performance and cost effectiveness. Three separate projects are described briefly below.

A research grant has recently (March 1978) been awarded to ASCE to develop a tentative interim standard for evaluating oxygen transfer equipment in water and wastewater. The implementing entity for ASCE is the previously mentioned Subcommittee on Oxygen Transfer Standards. This Workshop is the first activity undertaken by the Subcommittee with grant funds. The output of the Workshop will provide the foundation for further Subcommittee deliberations out of which will come the tentative interim standard. It is anticipated that a project period of 2-yr will be necessary to complete the task. EPA support over the 2-yr period will total $70,000. An important part of the final project report will be the recommendation of verification procedures for the tentative interim standard, which if carried out and the data incorporated would result in a final consensus standard.

Los Angeles County Sanitation Districts (LACSD) are currently conducting clean water oxygen transfer tests on six generic types of submerged air aeration systems. EPA is contributing $50,000 toward the cost of the test program in the form of a research contract. The objective of the project is to generate comparative oxygen transfer data on submerged aeration devices with a wide range of bubble formation sizes and widely-varying reported transfer efficiencies using the same test tank and identical test procedures throughout. The devices being evaluated include fine bubble jet aerators, dome diffusers, and tube diffusers; medium-size bubble static aerators; and fixed- and variable-orifice coarse bubble diffusers.

LACSD is carrying out the clean water tests at its Joint Water Pollution Control Plant in an outdoor tank 20 ft x 20 ft (6.1 m x 6.1 m) with a variable water depth of up to 25 ft (7.6 m) using the sodium sulfite reaeration procedure. Data collection is expected to be completed by the end of 1978. Oxygen transfer will be calculated using several popular analytical models.

Table 3. Estimated Annual Cost Breakdown for Desk-Top Upgrading Study of East Coast Plant

Aeration System	Capital Cost (July '77)	Aeration Power Load (kW)	Annual Power Cost‡	Total Annual Cost Including Amortization
Existing coarse bubble diffusers	--	2790	$730,000	$730,000
Replace with fine bubble diffusers	$1,350,000	915	$240,000	$371,000*
Replace with static aerators	$ 850,000	2055	$540,000	$615,000*

‡Based on 3¢/kW·hr.

* Based on 6-1/8% for 20-yr.

The test program consists of three runs at each of four liquid depths, 10, 15, 20 and 25 ft. (3.0, 4.6, 6.1, and 7.6 m). A different power level, varying from 0.3-1.5 hp delivered to the water/1000 ft^3 (0.008-0-0.04 kW/m^3), is employed for each of the three runs at any given depth. Reaeration is carried to DO equilibrium for each run.

Following the above clean water test program, process field evaluations are planned for promising submerged aeration systems. Equipment selection will be based on the results of the clean water studies. An activated sludge plant or plants with available parallel trains will be utilized to facilitate comparative assessment of process performance. At least 2-yr of field evaluation are planned at a tentative cost to EPA of $150,000.

In addition to noting process performance, two other facets of operation will be given close attention. First, attempts will be made to predict and then measure process oxygen transfer efficiencies using laboratory and/or field alpha determinations and the previously-generated clean water data. Second, operating requirements will be scrupulously documented, particularly as they relate to clogging, headloss buildup, and cleaning requirements (if any).

Discussion

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I agree that the first objective would be to adopt a standard procedure for the testing of aeration equipment under specified conditions. The 'non-steady state' method is generally used to determine the rate of reaeration of deoxygenated water by a given aeration device. Such a method has been successfully used for pilot- and full-scale testing of aerators in the UK. Because clean water can easily become contaminated by traces of surface-active substances which affect the rate of oxygen transfer, it is normal in the UK to add sufficient anionic detergent to achieve an average concentration of 5 mg/l during each test and so minimize the effect of possible contamination.

Section II. State of the Art Review

Review of Oxygen Transfer Model Refinements and Data Interpretation

7

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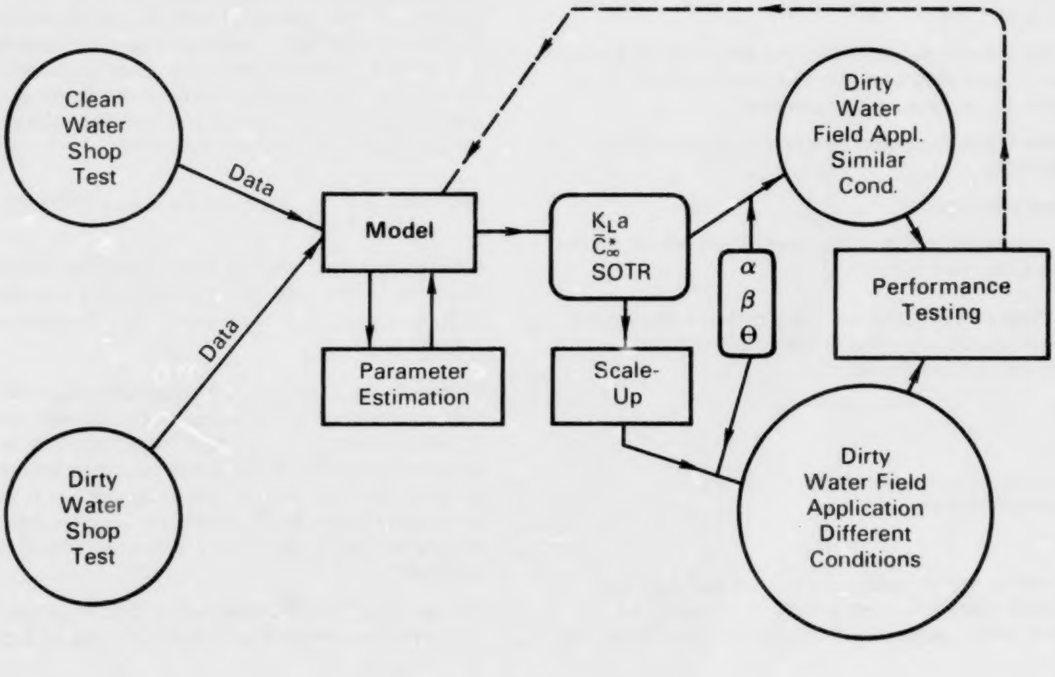
Introduction

An oxygen transfer model may be viewed as an equation or tool employed to characterize the performance of an aeration system. The model is necessary to translate the results of shop tests to field application and to characterize the performance of field systems. Figure 1 illustrates the relationship between models, shop tests, scale-up, and prediction of field performance.

The word "model" implies some consideration of scale and, indeed, scale-up of parameters obtained through the application of oxygen transfer models to laboratory or even to shop test systems does present a problem. Most workers hope to avoid this difficulty by employing shop test facilities that are sufficiently "similar" to the full-scale field installations.

This paper concentrates on a review of various oxygen transfer model refinements. Major emphasis is placed on modelling the non-steady state submerged aeration of clean water and application of the results to dirty water respiring systems. Although some consideration is given to scale-up, this topic is not covered in detail. Application of any model requires the use of some method of parameter estimation; e.g., "Direct Method", "Log Deficit Method" or Computer Estimation and, indeed, the method of parameter estimation may influence the results more than the choice of model (4). This topic is addressed in the next paper entitled "Oxygen Transfer Parameter Estimation".

Figure 1. Relationship between Models, Shop Tests Scale-up and Prediction of Field Performance


Clean Water Shop Test
Data
Model
K_La
$\bar{C}^*_\infty$
SOTR
Dirty Water Field Appl. Similar Cond.
Performance Testing
α
β
θ
Data
Parameter Estimation
Scale-Up
Dirty Water Shop Test
Dirty Water Field Application Different Conditions

Models of Oxygen Transfer Systems

Mass Transfer Fundamentals

The basic model for oxygen transfer in a dispersed gas-liquid system is given by:

$$\begin{bmatrix}\text{Rate of Mass Transfer}\\ \text{per Unit Volume of}\\ \text{Liquid}\end{bmatrix} = \begin{bmatrix}\text{Volumetric}\\ \text{Mass Transfer}\\ \text{Coefficient}\end{bmatrix}\begin{bmatrix}\text{Driving Force}\end{bmatrix}$$

According to the " two resistance theory" (18), the transfer rate can be expressed in terms of the overall transfer coefficients and resistances on either side of the interface.

$$\hat{W} = \hat{K}_L \hat{a}\,(\hat{C}^* - \hat{C}) \tag{1}$$

$$\hat{W} = \hat{K}_Y \hat{a}\,(\hat{Y} - \hat{Y}^*)\hat{p}_t \tag{2}$$

where the ^ superscript is employed to indicate the local nature of the quantities and:

W = transfer rate per unit volume in clean water, $m/L^3 \cdot t$

K_L = overall liquid phase mass transfer coefficient, based on the liquid resistance, L/t (clean water)

a = interfacial surface area per unit volume, L^{-1}

K_La = volumetric mass transfer coefficient, given conditions of geometry, mixing, temperature, etc. based on the liquid resistance, t^{-1} (clean water)

C = dissolved oxygen concentration in the liquid phase, m/L^3

C^* = dissolved oxygen saturation (equilibrium) concentration in the liquid phase, m/L^3

K_Y = overall gas phase mass transfer coefficient, $(m/L^2t)\cdot(L^2/f)$

Y^* = mole fraction oxygen in the gas phase in equilibrium with liquid dissolved oxygen concentration C, at total pressure p_t, dimensionless

Y = mole fraction oxygen in the gas phase, dimensionless

p_t = total pressure, f/L^2

K_Ya = volumetric mass transfer coefficient based on the gas film, $(m/L^3)\cdot(L^2/f)$

In the case of a slightly soluble gas, such as oxygen, the liquid resistance usually controls the transfer rate, and it can be shown that:

$$K_Ya \approx HK_La \tag{3}$$

where:

H = Henry's Law Constant, $(m/L^3)\cdot(L^2/f)$ defined so that:

$$C^* = HYp_t \tag{4}$$

This relationship is useful in relating gas side and liquid side mass balances arising in the analysis of oxygen transfer systems. It should be noted here that the mass transfer coefficient can be related to fundamental physical parameters by various theories such as the film, penetration, surface renewal, and combination theories. This paper is concerned with the application of mass transfer coefficients to modelling oxygen transfer systems and will not deal with the relative merits of these theories. The following development assumes only that there are two resistances to gas transfer and is not restricted to any single theory.

In applying Equation 1 to an oxygen transfer system, it must be remembered that this relationship applies to a particular point. In general, the total rate of oxygen transfer is of interest and can be evaluated by integrating the local transfer rate over the volume, V:

$$WV = \int_V \hat{W}\,dV = \int_V \hat{K}_L \hat{a}\,(\hat{C}^* - \hat{C})dV \tag{5}$$

Clearly, $\hat{K}_L$, $\hat{a}$, $\hat{C}^*$ and $\hat{C}$ may vary over the volume. Different assumptions made concerning the spatial and temporal variation of these quantities constitute the main differences between the various models applied to oxygen transfer systems.

Surface Aeration

In true or ideal surface aeration, mass transfer takes place at the free surface exposed to atmospheric pressure and oxygen content. Clearly then, $\hat{C}^*$ would be constant over the surface exposed to the gas phase and would be equal to the surface saturation value, C_S^*. The spatial variation of C will be influenced by mixing conditions and by biological uptake rate. Frequently, C is assumed to be uniform over the volume, V. The values of both $\hat{K}_L$ and $\hat{a}$ will vary over the transfer surface, being greatest in the vicinity of the aerator. Nevertheless, the usual approach is to consider a bulk average value of the transfer coefficient, K_La, defined so that the transfer relationship for ideal surface aeration becomes:

$$WV = \int_V \hat{K}_L \hat{a}\,(C_s^* - C)\,dV = K_La\,V\,(C_s^* - C) \tag{6}$$

where C_S^* and C are constant over the volume, V. Because of the averaging inherent in the definition of K_La, it is not suprising that it is a function of basin geometry.

Many pieces of equipment nominally referred to as surface aerators cause subsurface entrainment of air (9) with the result that $\hat{C}^*$ is not constant over the volume and the average value becomes somewhat greater than the surface saturation value. A subsurface aeration model based on an effective average saturation value would be more applicable to this situation.

The assumption of uniform C is crucial to the analysis of surface aeration. McWhirter (12) tested a 5-ft diameter surface aerator in a tank 20 ft in diameter and 27 ft in depth and reported that C values at a depth of 1 ft were on the order of 1 mg/l greater than the bulk average concentration. Since most of the transfer occurred near the surface, use of the bulk average concentration overestimated the driving force and tended to underestimate K_La by about 20%. A graphical integration procedure was suggested for analyzing situations of non-uniform C.

Submerged Aeration

In submerged aeration, mass transfer occurs throughout the volume and $\hat{C}^*$ will vary with depth because of progressive decreases in both hydrostatic pressure and oxygen mole fraction as the gas phase moves upward. Moreover, $\hat{K}_L\hat{a}$ and $\hat{C}$ could also vary over the tank volume. Many studies (5)(6)(16) have modelled submerged aeration based on the assumptions that:

1. The volumetric transfer coefficient, K_La, is constant over the tank volume.
2. Good mixing exists so that C is uniform over the tank volume.
3. Oxygen is the only gas transferred.

In the case of uniformly distributed submerged bubble aeration, these parameters might not vary appreciably. However, where air input is non-uniform or where a circulatory motion or turnover is induced in the tank, $\hat{K}_L\hat{a}$ and $\hat{C}$ could vary significantly over the volume. It should be noted here that spatial variations in $\hat{C}$ which could exist and significantly influence oxygen transfer in respiring systems might not be manifested in non-steady state clean water testing.

In some instances $\hat{C}^*$ has been assumed constant and equal to the surface saturation value so that the surface model given by Equation 6 would apply. Stanton and Bradley (16) have shown that this assumption seriously underestimates the true saturation concentration and yields inflated values of K_La. In other cases, an effective average saturation concentration was defined so that:

$$(7) \quad WV = K_La\int_0^{Z_d}(\hat{C}^*-C)\,dZ = K_La\,V\,(\bar{C}^*-C)$$

giving:

$$(8) \quad C^* = \frac{\int_0^{Z_d}\hat{C}^*\,dZ}{Z_d} = \frac{\int_0^{Z_d}H\hat{Y}\,(p_a+\gamma Z)\,dZ}{Z_d}$$

where:

$\bar{C}^*$ = effective average dissolved oxygen saturation concentration, m/L^3

A = horizontal area of tank, L^2

Z = vertical coordinate, L. Z = 0 at water surface.

Z_d = aerator submergence, assumed equal to tank depth, L

p_a = atmospheric pressure, f/L^2

γ = weight density of water, f/L^3

The accuracy with which the oxygen transfer rate, W, can be predicted depends greatly on the accuracy with which $\bar{C}^*$ can be determined. For all situations where $W > 0$, $\bar{C}^*$ cannot be measured directly but must be calculated based on Equation 8 and some known or assumed function of $\hat{Y}$ with depth.

In 1956, Oldshue (14) presented an equation relating $\bar{C}^*$ to oxygen content of the exit gas, hydrostatic pressure, and the surface saturation value:

$$(9) \quad \bar{C}^* = C_s^*[(Y_e/2Y_d) + (p_a+\gamma Z_d)/2p_a]$$

where:

Y_e = mole fraction of oxygen in the exit gas
Y_d = mole fraction of oxygen in the feed gas

Using Henry's Law to evaluate $C_s^* = HY_d\,p_a$ gives:

$$(10) \quad \bar{C}^* = (H/2)\,[Y_d(p_a+\gamma Z_d) + Y_e\,p_a]$$

which says that the effective average saturation concentration is equal to the average of the saturation concentration of the feed gas exposed to the submergence pressure and the exit gas exposed to atmospheric pressure.

Downing and Boon (5) in 1960 and Lister and Boon (10) in 1973 applied an equation slightly different from that of Oldshue to the analysis of submerged aeration. In the terminology used here, their equation can be written as:

$$(11) \quad \bar{C}^* = [(Y_d+Y_e)/2]\,(p_a+\gamma Z_d/n)H$$

where n = a number characteristic of the aeration bubble pattern. (These authors actually referred to the product H $(p_a+\gamma Z_d/n)$ as "a constant related to Henry's Constant".)

This relationship says that the effective average saturation concentration is equal to the saturation concentration of water exposed to the pressure Z_d/n in contact with gas containing a mole fraction of oxygen equal to the arithmetic average of the feed and exit gas compositions.

A third such relationship slightly different from both those of Oldshue and Downing-Boon can be developed by assuming that $\hat{Y}$ increases linearly with Z from $Y = \hat{Y}_e$ at the tank surface to $\hat{Y} = Y_d$ at the aerator submergence. Coupled with Equation 8, this assumption gives:

$$(12) \quad \bar{C}^* = H\left\{Y_d[p_a + 2/3(\gamma Z_d)] + Y_e\,[p_a + 1/3(\gamma Z_d)]\right\}/2$$

which says that the effective average saturation concentration corresponds to the arithmetic average of the feed gas exposed to a pressure at 2/3 the depth and exit gas exposed to a pressure at 1/3 of the depth.

Yet another such relationship can be developed by applying a few convenient approximations to the differential oxygen balance written for the rising gas stream. An outline of this development is given in Attachment A. The resulting expression for the mole fraction of oxygen in the gas phase as a function of depth and liquid phase dissolved oxygen concentration is (Equation A.11):

$$(13)\quad \hat{Y} = C/Hp_t + [Y_d(p_d/p_a) - C/Hp_t] \exp\left[\frac{-K_La}{\phi_b} \cdot \frac{(Z_d - Z)}{Z_d}\right]$$

where:

$p_t = p_a + \gamma Z$, f/L^2

$p_d = p_a + \gamma Z_d$, f/L^2

ϕ_b = correction number, t^{-1}

$\phi_b = (M_o/M_a)\{(\bar{\rho}_a Q_a)/[H(p_a + \gamma Z_d/2)V]\}$, t^{-1}

M_o = molecular weight of oxygen

M_a = average molecular weight of air

Q_a = volumetric flow rate of air at standard conditions, L^3/t

$\bar{\rho}_a = (p_a + Z_d/2)M_a/RT$, m/L^3

Using Equation 13 to evaluate Y in Equation 8 and integrating gives:

$$(14)\quad \bar{C}^* = C + \frac{(HY_dp_d - C)[1 - \exp(-K_La/\phi_b)]}{K_La/\phi_b}$$

A comparison of Equations 10, 11, 12, and 14 can be made for the case where:

Z_d = 10 ft

H = 3.0508 mg/l·psi

C_s = 9.2 mg/l

C = 2 mg/l

Y_d = 0.21

Y_e = 0.19

K_La = 0.15 min^{-1}

$\rho_a Q_a/A$ = 197 g/min·m^2

ρ_a = mass density of air at standard conditions, mL^3

By Equation 10 (Oldshue), $\bar{C}^*$ = 10.15 mg/l.
By Equation 11 (Downing-Boon), $\bar{C}^*$ = 10.06 mg/l (with n = 2).
By Equation 12 (Linear M.F.), $\bar{C}^*$ = 10.11 mg/l.
By Equation 14, $\bar{C}^*$ = 11.6 mg/l

The values of the effective average saturation concentration predicted by Equations 10, 11, and 12 agree very closely. However, the value given by Equation 14 seems unrealistically high. This high value is probably caused by the convenient approximations made in the development of Equation 14.

Models Applied to Non-Steady State Clean Water Tests

Here, the overall oxygen balance on the liquid phase becomes:

$$(15)\quad \underset{WV}{\text{(Rate Transferred)}} = \underset{V(dC/dt)}{\text{(Rate of Accumulation)}}$$

Surface Aeration

Combining Equation 15 with the surface transfer model given by Equation 6 results in:

$$(16)\quad dC/dt = K_La(C_s^* - C)$$

$$(17)\quad \int_{C_o}^{C} dC/(C^* - C) = K_La \int_o^t dt$$

$$(18)\quad \ln[(C_s^* - C)/C_s^* - C_o)] = -K_Lat$$

$$C = C_s^* - (C_s^* - C_o) \exp(-K_Lat)$$

Equations 18 and 19 are routinely employed to estimate K_La from non-steady state test data. Although this model predicts the dissolved oxygen saturation concentration attained at infinite time to be equal to C_s^*, many surface aerators yield higher values. This indicates that subsurface transfer is taking place and that a subsurface model might be applied.

Submerged Aeration

Combining Equation 15 with the subsurface transfer given by Equation 7 results in:

$$(20)\quad dC/dt = K_La(\bar{C}^* - C)$$

Most analyses of non-steady state submerged aeration have applied this relationship by considering the average equilibrium saturation concentration, $\bar{C}^*$, to be constant and equal to the average saturation concentration attained at infinite time $\bar{C}_\infty^*$, even though the value of C^* clearly increases during the course of a non-steady state test as the oxygen content of the exit gas increases.

Thus, the model usually applied to analyze non-steady state submerged aeration is:

$$(21)\quad dC/dt = K_La'(C^* - C)$$

or:

$$(22)\quad \ln[(\bar{C}_\infty^* - C)/(\bar{C}_\infty^* - C_o)] = -K_La't$$

or:

(23) $$C = \bar{C}^*_\infty - (\bar{C}^*_\infty - C_o) \exp(-K_L a' t)$$

where:

$\bar{C}^*$ = average dissolved oxygen saturation concentration attained at infinite time, m/L^3

$K_L a'$ = apparent volumetric mass transfer coefficient, t^{-1}

This practice overestimates the average driving force during the non-steady state test and estimates an apparent volumetric mass transfer coefficient, $K_L a'$, somewhat smaller than the true $K_L a$. A cogent argument for the use of this model is that it fits data very well and, at first glance, this agreement with observed data appears to deny the fact that $\bar{C}^*$ is really a function of time. However, it can be shown that any of the four relationships (Equations 10, 11, 12 or 14) between $\bar{C}^*$ and the exit gas composition discussed previously, coupled with Equation 20 and the assumption of constant gas rate, will produce equations identical in form with Equations 21 and 23. The following development illustrates this for the Downing-Boon relationship (5). An overall non-steady state oxygen balance gives:

Rate In - Rate Out = Rate of Accumulation

(24) $$(M_o/M_a)[\rho_a Q_a (Y_d - Y_e)] = V(dC/dt)$$

Here an approximation is introduced by the assumption of constant gas rate; i.e., the mass gas rate, $\rho_a Q_a$, emitted from the aerator is equal to the mass gas rate exiting at the tank surface. A similar development based on variable gas rate is given in Attachment B and leads to a different mathematical form for the function of C with time (Equation B.4).

For the conditions of $t = \infty$, $Y_d = Y_e$ and $\bar{C}^* = \bar{C}^*_\infty$, Equation 11 becomes:

(25) $$\bar{C}^*_\infty = Y_d(p_a + \gamma Z_d/n)H$$

and using this to eliminate H in Equation 11 gives:

(26) $$\bar{C}^* = [(Y_d + Y_e)/2Y_d]\,\bar{C}^*_\infty$$

Using Equation 24 to eliminate Y_e gives:

(27) $$\bar{C}^* = \bar{C}^*_\infty [1 - (M_a V/2M_o \rho_a Q_a Y_d)(dC/dt)]$$

Substituting this into Equation 20 and solving for dC/dt results in:

(28) $$dC/dt = [K_L a/(1 + K_L a/\phi_d)](\bar{C}^*_\infty - C)$$

where:

$$\phi_d = \text{correction number} = \frac{2M_o \rho_a Q_a}{M_a H(p_a + \gamma Z_d/n)V}, \; t^{-1}$$

The correction number can also be related to the oxygen transfer efficiency at zero dissolved oxygen, OTE_o, by:

(29) $$\phi_d = K_L a\,(2 - OTE_o)/OTE_o$$

Comparison of Equations 21 and 28 shows that the true and apparent values of $K_L a$ are related by:

(30) $$K_L a' = K_L a/(1 + K_L a/\phi_d)$$

The results of similar developments based on Equations 10, 12, and 13 are summarized in Table 1. It can be seen that, although the correction number is defined in a slightly different manner for each model, the relationships between the true and apparent values of $K_L a$ are very similar for the models resulting from the Downing-Boon, Oldshue and Linear Mole Fraction equations. The resulting values of $\bar{C}^*_\infty$ are also similar for these three models as the Oldshue and Linear Mole Fraction models predict mid-depth saturation and the Downing-Boon model predicts 1/n th depth saturation. The model based on the integrated non-linear mole fraction profile yields a $\bar{C}^*_\infty$ corresponding to aerator submergence. This is unrealistic and is a consequence of the convenient approximations made in the development of this model. Typical values of $\bar{C}^*_\infty$ observed in clean water tests fall between the one-quarter and mid-depth values (15) (17).

Stanton and Bradley (16) have indicated that the assumptions of uniform $K_L a$ and $\hat{Y} = Y_d$ result in values of $\bar{C}^*_\infty$ corresponding to mid-depth. Under these assumptions, Equation 8 becomes:

(31) $$\bar{C}^*_\infty = \frac{Y_d H \int_0^{Z_d} (p_a + \gamma Z)\,dZ}{Z_d} = HY_d(p_a + \gamma Z_d/2)$$

However, at the steady state saturation condition, $\hat{Y}$ will not be uniform with depth, but will decrease from Y_d at the aerator to a minimum value at an intermediate depth and then increase to Y_d at the surface. This is induced by the hydrostatic pressure variation which causes oxygen absorption in the lower region of the tank and desorption in the upper region. Thus, an argument can be made for not removing $\hat{Y}$ from the integrand of Equation 8 and for accepting values of $\bar{C}^*_\infty$ corresponding to other than the mid-depth value even though the model is based on the assumption that $K_L a$ is uniform with depth.

From the viewpoint of parameter estimation, the Downing-Boon model can be fit to experimental data by adjusting two parameters, $K_L a$ and n or $K_L a$ and $\bar{C}^*_\infty$. The three other models are suited to only one parameter ($K_L a$) estimates. This means that, for values of $\bar{C}^*$ other than the mid-depth value, the two parameter Downing-Boon model will produce a better fit to non-steady state test data.

Typical values of the correction number are on the order of 1.5 min^{-1} whereas typical values of $K_L a'$ are on the order of 0.15 min^{-1}. According to the relationships given in Table 1, therefore, typical values of the true $K_L a$ would be about 10% greater than those of the apparent $K_L a$.

Table 1. Relationships Between "True" and "Apparent" Values of K_La for Various Non-Steady State Test Models

Relationship Between $\bar{C}^*$ and Y_e	Downing-Boon Equation 11	Oldshue Equation 10	Integrated Linear M.F. Equation 12	Integrated Non-Linear M.F. Equations 8 and 13
Correction Number, t^{-1}	ϕ_d $\frac{2M_o\rho_aQ_a}{M_a\,HV\,(p_a+/Z_d/n)}$	ϕ_o $\frac{2M_o\rho_aQ_a}{M_a\,HV\,p_a}$	ϕ_L $\frac{2M_o\rho_aQ_a}{M_a\,HV\,(p_a+\gamma Z_d/3)}$	ϕ_b $\frac{M_o\bar{\rho}_aQ_a}{M_a\,HV(p_a+\gamma Z_d/2)}$
$\bar{C}^*_\infty$	$HY_d\,(p_a+\gamma Z_d/n)$	$HY_d\,(p_a+\gamma Z_d/2)$	$HY_d\,(p_a+\gamma Z_d/2)$	$HY_d\,(p_a+\gamma Z_d)$
K_La	$K_La'/(1-K_La'/\phi_d)$	$K_La'/(1-K_La'/\phi_o)$	$K_La'/(1-K_La'/\phi_L)$	$-\phi_b \ln(1-K_La'/\phi_b)$

Models Applied to Oxygen Transfer in Steady State Respiring Systems

In this case, the overall liquid phase oxygen balance becomes:

Rate of Transfer = Rate of Biological Uptake

(32) $$\int_v \hat{W}_f\,dV = \int_v \hat{R}\,dV$$

where:

$\hat{W}_f$ = transfer rate per unit volume under the dirty water conditions, $m/L^3 \cdot t$

$\hat{R}$ = oxygen uptake rate per unit volume due to biological action, $m/L^3 \cdot t$

For submerged aeration, the transfer model is written as:

(33) $$\int_v \hat{W}_f\,dV = \int_v \hat{\alpha}\hat{K}_L\hat{a}\,(\beta\hat{C}^* - \hat{C})dV = \int_v \hat{R}dV$$

Assuming uniform distribution over the volume gives:

(34) $$W_fV = \alpha K_La\,(\beta\bar{C}^* - C)\,V = RV$$

where:

α = dirty water K_La/clean water $K_La = (K_La)_f/K_La$

β = dirty water $\bar{C}^*$/clean water $\bar{C}^* = \bar{C}^*_f/\bar{C}^*$

and it is assumed that:

(35) $$(\bar{C}^*_f/\bar{C}^*) = (\bar{C}^*_{\infty f}/\bar{C}^*_\infty) = (C^*_{sf}/C^*_s) = \beta$$

Here $\beta\bar{C}^*$ may be significantly less than $\beta\bar{C}^*_\infty$ and could be determined by the application of one of the equations previously discussed; e.g., Equation 10, 11, 12, or 14 with Y_e evaluated by Equation 24 with R = dC/dt.

Alternately, an apparent K_La could be employed in conjunction with the driving force based on $\bar{C}^*_\infty$:

(36) $$W_f\,V = \alpha'\,K_La'\,(\beta\bar{C}^*_\infty - C)\,V = RV$$

where:

α' = dirty water K_La'/clean water $K_La' = (K_La)_f/K_La'$

Either approach is valid. However, the true K_La cannot be employed in conjunction with the driving force based on $\bar{C}^*_\infty$.

The steady state model is deceptively simple. The difficulty in applying it arises in the measurement of R, α, and β. In the development of Equations 34 and 36, the values of R and α were assumed to be independent of time. However, because of substrate depletion, surfactant removal, and changes in biomass character during the test, the values of R and α may change. In addition, there is some indication that R may be influenced by the degree of agitation (3). Various methods based on measurement of oxygen uptake from the liquid phase have been employed to evaluate R during steady state testing (1)(2)(13). Lister and Boon (10) employed a gas phase oxygen balance to evaluate R and obtained excellent results.

Overshadowing the problems related to the measurement of R and α, is the possibility the K_La may be dependent on uptake rate in respiring systems. Albertson and DiGregorio (1) presented data indicating that K_La increased markedly with increasing biological uptake rates. Subsequent discussions (7) (11) discredited the mechanism which they proposed and claimed their results to be artifactual. However, the discussions could not refute the data completely. If the results of Albertson and DiGregorio are valid, it seems that clean water testing would be of limited usefulness.

Field Aplication to Dirty Water Respiring Systems at Steady State

In order to relate clean water to field conditions, it is useful to define the following:

τ = Temperature Adjustment Factor for Dissolved Oxygen Saturation

$\tau = \bar{C}^*_{\infty T} / \bar{C}^*_{\infty 20}$

and it is assumed that temperature influences on C^*_∞ and C^*_s are identical so that,

$$\tau = \bar{C}^*_{\infty T}/\bar{C}^*_{\infty 20} = C^*_{sT}/C^*_{s20} \tag{37}$$

$$F = \text{Exit Gas Correction Factor} = \bar{C}^*/\bar{C}^*_\infty$$

and it is assumed that the effect of exit gas depletion is identical for both clean and dirty water conditions so that:

$$F = \bar{C}^*/\bar{C}^*_\infty = \bar{C}^*_f/\bar{C}^*_{\infty f} \tag{38}$$

$$\theta^{(T-20)} = (K_L a)_{fT}/(K_L a)_{f20}$$

$$\theta'^{(T-20)} = (K_L a')_{fT}/(K_L a')_{f20}$$

Using these relationships in the following identity:

$$\bar{C}^*_{\infty 20} (\bar{C}^*_{\infty T}/\bar{C}^*_{\infty 20}) (\bar{C}^*_{\infty f}/\bar{C}^*_{\infty T}) (\bar{C}^*_f/\bar{C}^*_{\infty f}) = \bar{C}^*_f \tag{39}$$

gives:

$$\bar{C}^*_{\infty 20} \tau \beta F = \bar{C}^*_f \tag{40}$$

Equation 34 can be written in terms of the true $K_L a$ and $\bar{C}^*_{20}$ as:

$$OTR_f = (K_L a)_{20} \alpha' \theta^{(T-20)} (\tau \beta F \bar{C}^*_{\infty 20} - C) V \tag{41}$$

where:

ORT_f = Field Oxygen Transfer Rate = $W_f V$, m/t

An equivalent expression can be written in terms of the apparent $K_L a$ as:

$$ORT_f = (K_L a')_{20} \alpha \theta'^{(T-20)} (\tau \beta \bar{C}^*_{\infty 20} - C) V \tag{42}$$

The exit gas depletion factor is evaluated based on the relationship between $\bar{C}^*$ and $\bar{C}^*_\infty$ employed in the determination of $K_L a$. For the Downing-Boon relationship a combination of Equations 24 and 26 gives, with $W_f = R = dC/dt$:

$$F = 1 - (R/\Phi_d \bar{C}^*_\infty) \tag{43}$$

Summary and Conclusion

This review has indicated that the phenomenon of oxygen transfer in dispersed gas-liquid systems defies a completely rigorous analysis. Attempts at such analyses invariably require certain approximations and assumptions and it is not clear whether the additional complexity is justified by any improved accuracy of the resulting models.

In the case of submerged aeration, two approaches to modelling the oxygen transfer process have been reviewed. The conventional approach neglects the effect of exit gas depletion and is based on an "apparent $K_L a$". A modified, somewhat more realistic approach considers the influence of gas-side oxygen depletion and is based on a "true $K_L a$". However, if scale-up considerations are ignored (i.e., gas rates, submergence, and spacing are assumed to be equivalent for test and field conditions) both approaches can be applied to predict field oxygen transfer rates. Field predictions based on the conventional model involve an "apparent alpha", whereas predictions based on the modified model involve a "true alpha" and errors associated with the estimation of these parameters may overshadow any real differences between the field oxygen transfer rate predictions given by each approach.

If the modelling effort is extended to include scale-up to allow for varying gas rates and submergences, it appears more logical to employ the modified model based on the "true $K_L a$". At any rate, various versions of logical modified models considering the influence of exit gas depletion have appeared in the literature and a concensus standard will have to deal with both types of models.

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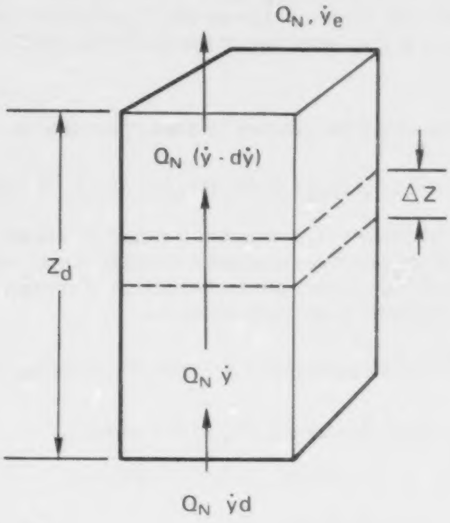
Attachment A: Fundamental Relationships in Submerged Aeration

Differential Oxygen Balances on Section of Thickness ΔZ



Gas Side Balance

Rate In − Rate Out − Rate Transf. = Rate of Accum.

(A.1) $\rho_N Q_N d\dot{y} - WAdZ = AdZ\rho_N f_N(\partial\dot{y}/\partial t)$

where:

ρ_N = mass density of nitrogen, m/L^3
Q_N = volumetric flow rate of nitrogen, L^3/t
$\rho_N Q_N$ = mass flow rate of nitrogen, m/t
A = horizontal area, L^2
f_N = fraction of tank volume occupied by nitrogen
$\dot{y}$ = mass ratio of oxygen to nitrogen in gas phase
W = transfer rate per unit volume, m/L^3t

Liquid Side Balance

Rate in by Transfer = Rate of Accumulation

(A.2) $WAdZ = AdZ\ (\partial C/\partial t)$

Transfer Relationship

(A.3) $W = K_L a(C^* - C) = K_Y a p_t\ (Y - Y^*)$

Henry's Law

(A.4) $C^* = Hp_tY$

Relationship between Mass Ration and Mole Fraction

(A.5) $Y = \dot{y}/(\dot{y}+J) \qquad J = M_o/M_N$

Combining Equations A.1, A.3, A.4, and A.5 gives:

(A.6) $\rho_N Q_N\, \partial\dot{y}/\partial Z = AK_L a\{Hp_t[\dot{y}/(\dot{y}+J)]-C\}+Af_N\rho_N\,\partial\dot{y}/\partial t$

Some convenient approximations are introduced here to facilitate the solution of Equation A.6.

Pseudo steady state conditions are assumed so that the term containing $\partial y/\partial t$ can be neglected. Rough calculation based on typical non-steady state test data indicate that this term in on the order of 5% at each of the other terms.

(A.7) $\rho_N Q_N(d\dot{y}/dZ) = AK_L a\left\{Hp_t[\dot{y}/(\dot{y}+J)]-C\right\}$

Stanton and Bradley (16) solved this equation for the special case where C = O, subject to the boundary conditions:

$\dot{y} = \dot{y}_e$ at $Z = O$ (surface)
$\dot{y} = \dot{y}_d$ at $Z = Z_d$ (at aerator)

and obtained:

(A.8) $\dot{y}_d - \dot{y}_d\,(1-OTE_o) + J\,\ln[1/(1-OTE_o)] = (HA/\rho_N Q_N)\,K_L a\, Z_d\,(p_a + \gamma Z_d/2)$

where OTE_o = oxygen transfer efficiency at zero DO.

An analytical solution for the general case where $C > 0$ requires the approximation that the total mass rate of gas, $\rho_a Q_a'$, passing upward through the tank is constant so that:

(A.9) $\rho_N Q_N\, dy \cong (M_o/M_a)\,\rho_a Q_a\, dY$

Equation A.7 can then be written as:

(A.10) $(M_o/M_a)\,\rho_a Q_a\,(dY/dZ) = AK_L a\,(Hp_t\, Y - C)$

This equation can be solved by rearranging to a first order linear form and letting $\rho_a = (p_t\, M_a)/RT$, subject to the boundary conditions:

at $Z = Z_d$, $Y = Y_d$ and $p_t = p_d$
at $Z = Z$, $Y = Y$ and $p_t = p_t$

to give:

(A.11) $Y = C/Hp_t + [Y_d(p_d/p_a) - C/Hp_t]\exp\left(\dfrac{-K_L a}{\phi_b}\cdot\dfrac{Z_d - Z}{Z_d}\right)$

where:

ϕ_b = correction number $= (M_o/M_a)\{(\bar{\rho}_a Q_a)/[H(p_a+\gamma Z_d/2)V]\}, t^{-1}$
$\rho_a = (\rho_a+\gamma Z_d/2)M_a/RT,\ m/L^3$

Lakin and Salzman developed a similar equation from consideration of bubble phenomena (8).

Substitution of Equation A.11 into Equation 8 and integrating gives:

$$\text{(A.12)} \qquad \bar{C}^{*} = C + \frac{(HY_d p_d - C)[1 - \exp(-K_L a/\phi_b)]}{K_L a/\phi_b}$$

Combining this with the result of the liquid side balance and the transfer equation, $W = dC/dt = K_L a\,(\bar{C}^{*} - C)$, gives:

$$\text{(A.13)} \quad dC/dt = K_L a \{[1 - \exp(-K_L a/\phi_b)]/(K_L a/\phi_b)\}(HY_d p_d - C)$$

which is of the form:

$$dC/dt = K_L a'\,(\bar{C}^{*}_{\infty} - C)$$

This result is included in Table 1.

Equation A.10 can also be solved by letting $C = H_p y^{*}$ and recognizing that:

$$dY/(Y-Y^{*}) = d(Y-Y^{*})/(Y-Y^{*})$$

to give, subject to the boundary conditions that:

$$\text{at } Z = O,\ Y = Y_e \text{ and at } Z = Z_d,\ Y = Y_d$$

$$\text{(A.14)} \qquad (M_o/M_a)\rho_a Q_a (Y_d - Y_e) =$$

$$K_L a\, HV\,(p_a + \gamma Z_d/2) \underbrace{\left[\frac{(Y_d - Y_d^{*}) - (Y_e - Y_e^{*})}{\ln[(Y_d - Y_d^{*})/(Y_e - Y_e^{*})]}\right]}_{\substack{\text{Log mean} \\ \text{gas phase} \\ \text{driving force}}}$$

and recognizing that:

$$(M_o/M_a)\,\rho_a Q_a\,(Y_d - Y_e) = V(dC/dt)$$

gives:

$$\text{(A.15)} \qquad dC/dt = K_L a\, H\,(p_a + \gamma Z_d/2)\ \text{(Log mean gas phase driving force)}$$

or in terms of the liquid phase driving force:

$$\text{(A.16)} \quad dC/dt = K_L a \left[\frac{(p_{md}/p_d)(C_d^{*} - C) - (p_{md}/p_a)(C_e^{*} - C)}{\ln[(p_a/p_d)(C_d^{*} - C)/(C_e^{*} - C)]}\right]$$

where p_{md} = pressure at mid-depth, indicating that the liquid phase driving force must be corrected by the appropriate pressure ratios before applying the log-mean convention.

Attachment B: Variable Gas Rate Modification

The development of the non-steady state test model given by Equation 28 is based on the approximation that the mass rate of gas, $p_a Q_a$, is constant with depth. The following is an outline of the parallel development which assumes that the mass rate of nitrogen is constant with depth.

The non-steady state oxygen balance is written as:

$$\text{(B.1)} \quad (M_o/M_a)(\rho_{ad} Q_{ad} Y_d) - (M_o/M_a)(\rho_{ae} Q_{ae} Y_e) = V(dC/dt)$$

where the subscript d denotes the values at the aerator submergence and the subscript e denotes the values at the tank surface. Since the mole fraction of nitrogen is 1-Y, the nitrogen balance is written as:

$$\text{(B.2)} \quad (M_N/M_a)(\rho_{ad} Q_{ad})(1 - Y_d) = (M_N/M_a)(\rho_{ae} Q_{ae})(1 - Y_e)$$

Using B.2 to eliminate $\rho_{ae}\,Q_{ae}$ in B.1 gives:

$$\text{(B.3)} \qquad Y_e = [Y_d - K\,(dC/dt)]/[1 - K\,(dC/dt)]$$

where $K = V/[(M_o/M_a)(\rho_{ad} Q_{ad})]$.

Using B.3 to eliminate Y_e in Equation 26 and substituting the result into Equation 20 leads to a quadratic first order differential equation which can be solved by the method of substitution. The solution, for $Y_d = 0.21$, is:

$$\text{(B.4)} \quad \left(\frac{A - Be^{X}}{A - Be^{X_o}}\right)^{\left(\frac{A+B}{A}\right)} \exp[(B/A)(X_o - X)] = \exp(-K_L a t)$$

where X is a function of C given by:

$C + (4.7619/K_L aK) - 2.881\,\bar{C}^{*}_{\infty} = 5.9857(\bar{C}^{*}_{\infty}/K_L aK_1)^{0.5}\sinh X$ and X_o is evaluated at $C = C_o$. $A = 9.5238/\sqrt{K}$, and $B = 5.9857\sqrt{K_L a\,\bar{C}^{*}_{\infty}}$. This model has been fit to non-steady state test data by the non-linear least squares computer technique, minimizing Σt residuals.

Oxygen Transfer Parameter Estimation

8

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Introduction

The analysis of non-steady state oxygen transfer data has received much attention in the recent literature. The objective of data analysis is usually to estimate the value of the mass transfer coefficient, K_La, from experimental measurement of dissolved oxygen concentration with time. A number of graphical and numerical procedures have been proposed to estimate K_La. The differences among many of these procedures result from the use of a variety of values for C^*, the oxygen saturation values used to compute the driving force for the mass transfer. Other procedures allow direct estimation of C^* from the experimental data.

There are two basic reasons for such a large number of data analysis methods. First is the fact that in many aeration systems C^* is not constant, but varies with time and over the depth of the aeration tank. Different methods of accounting for this variation have resulted in different methods of data analysis. Secondly, the integrated form of the fundamental mass transfer equation can be expressed both in a linear form (ln dissolved oxygen deficit vs time) and in a non-linear form (dissolved oxygen concentration vs time). Estimates of the mass transfer coefficient often vary with the assumed form of the model.

The purpose of this paper is threefold:

1. To review the fundamental statistical concepts and requirements for fitting equations to data
2. To review and evaluate the procedures used to estimate overall oxygen transfer coefficients and oxygen saturation values
3. To offer some observations concerning the statistical merits of the various methods and to suggest some criteria for a unified method of analyzing experimental data from non-steady state oxygen transfer testing.

Fitting Equations to Experimental Data

There are three immediate areas of concern when one is faced with the problem of fitting a mathematical model to a given set of data. The first is whether the equation being proposed does in fact correctly model the system under study. The second is how to select the "best" estimates of the parameters in the proposed model. The third is to determine the precision of the computed parameter estimates.

The first question has been discussed in the previous paper. In general the guiding principles here are to use a model that is consistent with one's theoretical knowledge of the system and yet is simple enough to be verified by experimental data. Because the topic of this paper is parameter estimation, it will be assumed that the model used is correct. However, one should always maintain an awareness of patterns or indications in the data analysis that may suggest model inadequacy.

Fundamental Oxygen Transfer Model

There seems to be wide agreement in the literature on the general form of the oxygen transfer rate equation:

$$W = dC/dt = K_La\,(C^* - C) \tag{1}$$

where W = transfer rate per unit volume, K_La = volumetric mass transfer coefficient, and C^* = dissolved oxygen saturation concentration in the liquid phase. In most cases, K_La and C^* are assumed to be constant (or relatively constant) over time. By specifying the initial condition that $C = C_o$ at $t = 0$, Equation 1 can be integrated to yield a number of forms:

Thus in terms of log deficit:

$$\ln(C^*-C) = \ln(C^*-C_o)-K_Lat \tag{2}$$

or in terms of dissolved oxygen concentration:

$$C = C^*-(C^*-C_o)\exp(-K_Lat) \tag{3}$$

Most non-steady state aeration tests are performed in such a manner that the bulk oxygen concentration, C, is measured as a function of time. Generally C is assumed to be uniform over the tank volume. These data are then modified to the form of Equations 1, 2, or 3 so that estimates of the parameters C_o, K_La, and/or C^* can be obtained. While C and t are the data to which the equations are fit, other variables are measured and/or controlled during a non-steady transfer test. These variables include temperature, relative humidity, and barometric pressure which allow correction to standard conditions. Other variables such as gas flow rate, aeration speed, exit gas composition, power input, tank geometry, and water/wastewater characteristics are also recorded to determine their effect on transfer.

Method of Least Squares

The task of parameter estimation is one of selecting the values of the parameters C_o, K_La, and C^* in the model so that the calculated or "fitted" values of dissolved oxygen concentration agree as closely as possible with the observed or measured concentrations. The method of least squares is a computational procedure for finding those "best" parameter values. Specifically, the least squares criterion states that the parameter values should be selected so that the sum of the squares of the deviations between observed and calculated values be a minimum. Thus, using Equation 3 as an example, the least squares criterion says that the best estimates of the values of C_o, K_La, and C^* are those which minimize

$$S = \sum_i (C_{i,\,obs} - C_{i,\,calc})^2 = \text{minimum} \tag{4}$$

where S is the sum of squares function. The values of C_o, K_La, and C^* obtained from the solution of Equation 4 are called the least squares estimates.

For models that are linear in the parameters, the values of the least squares estimates can be calculated explicitly (9). The method involves solving a set of simultaneous linear equations that express the least square parameter values as a function of the data. Polynomials are examples of linear models. Equation 2 also is a linear model when the dependent variable is the logarithm of the dissolved oxygen deficit.

For models that are non-linear in the parameters an explicit algebraic solution to Equation 4 is not possible. Rather an iterative procedure that searches over many combinations of parameter values is required to find the minimum. The search procedure is best accomplished numerically and efficient schemes have been programmed and are readily available (15). An example of a non-linear model is the classic BOD equation (4). Also, Equation 3 is non-linear in the parameters when C, the dissolved oxygen concentration, is the dependent variable.

A review of linear and non-linear least squares regression techniques can be found in a number of texts (3) (9) (16) (24).

Error Structure of the Data

Implicit in the use of the method of least squares is the assumption that each observation has the same inherent error. It often happens in reality that data do not behave in this manner. When the magnitude of the experimental error varies with the value of the dependent variable, a weighted least squares method should be used to analyze the data. Normally the weighting factors are assumed to be inversely proportional to the variance of the observed data value. Thus, observed values with large error carry a small weight while those with small errors are weighted more heavily.

A number of techniques are available for determining the error structure of a set of data. One method is to make replicate observations of the parameters at fixed values of the independent variables and then calculate the error variance directly. Another technique involves an examination of the residuals. A residual is defined as the difference between an observed and a calculated (from a model) data value. It is what remains of the observation after the part explained by the model is taken away. If the model fitted to the data is correct, the residuals should correspond to the uncontrollable factors causing experimental error. Thus, they should be random, normally distributed and have a constant variance. If they do not exhibit these properties but show a trend, then either the model or the method of analysis should be altered to accommodate this trend. The examination of residuals is a powerful diagnostic tool for checking model adequacy and error structure of the data (9).

If it is determined that the experimental error does not have constant variance, a weighted least squares procedure is required and two procedures can be used. First, the data may be transformed to a new variable having constant variance. For example, data having constant percent error can be transformed to data having constant error by taking logarithms. A number of other transformation schemes have been suggested by Davies (8). A method of obtaining weighting factors has been described by Box and Hill (5) when replicate observations are available.

Summary — Model/Errors

From the above discussion it can be seen that the form of the model *and* the error structure of the data *together* determine the appropriate method of data analysis. The model determines whether linear or non-linear least squares is necessary, and the data determine whether a weighted or unweighted analysis is required. This interrelationship has been described by Boyle *et al* (6) and is summarized in Table 1.

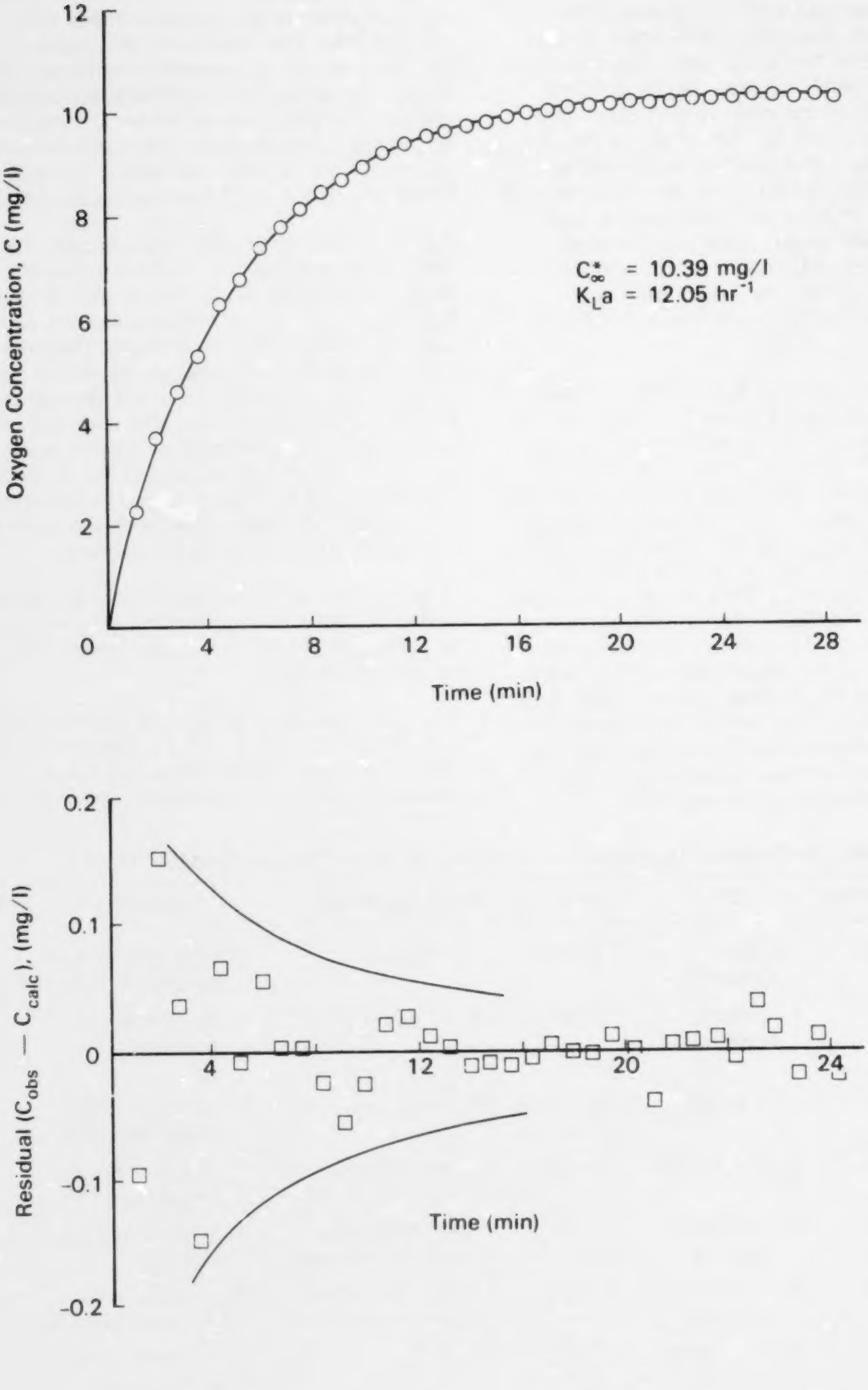
Table 1. General Classes of Parameter Estimation Problems and Least Squares Calculation (6)

Error Structure of Data / Form of Model	Equal Variance	Unequal Variance
Linear	Unweighted Linear Least Squares	Weighted Linear Least Squares
Non-linear	Unweighted Non-linear Least Squares	Weighted Non-linear Least Squares

Example

To illustrate the concepts discussed above, an example will be given using the data of Gilbert and Chen (10). First, Equation 3 was fit using non-linear least squares. The data, fitted values, and residuals are plotted in Figure 1. Then, using the value of C* from that fit, the dissolved oxygen deficits, C*-C, were computed and their logarithms fit to Equation 2 using a linear least

Figure 1. Data of Gilbert and Chen (10) — Equation 3 Exponential Method



squares procedure. The transformed data (ln deficit), fitted values, and residuals are plotted in Figure 2.

From the plot of oxygen concentration vs time in Figure 1, it is seen that the fitted curve appears to describe the data well. However, examination of the residuals shows that for times less than about 8 min (DO < 8 mg/l), the scatter in the residuals is much larger than in the rest of the data. This indicates that errors in C are not constant, but are large for low C's (less than about 8 mg/l) and small for higher values of concentration. This observation in the data is consistent with field experience in measuring dissolved oxygen concentrations in a non-steady state transfer experiment. Thus, low values of C should carry a smaller weight than the rest of the data and a weighted analysis that accounts for this error structure should be employed.

Inspection of the fit of Equation 2 in Figure 2 shows that the model describes the data well for the first 20 min of the experiment (for oxygen concentrations within about 0.3 mg/l of equilibrium). After this time, the oxygen concentration is so close to equilibrium that the error in the deficit becomes a large percentage of the actual deficit. This larger relative error is magnified by the logarithmic transformation, and its effect is graphically shown in Figure 2. Many investigators have suggested truncating the data at about 90% of equilibrium (in this case at t = 14 min) to avoid this problem. The merits of truncation will be considered later in the paper. Note finally that the residuals at times less than 20 min are well behaved. Thus, although there are error problems with concentrations close to equilibrium, the logarithmic deficit transformation appears to provide a proper weighting for the data up to that point.

Methods of Data Analysis — Non-Steady State Oxygen Transfer

A variety of graphical and numerical procedures have been proposed to analyze non-steady state oxygen transfer data. The objective of the analysis is to estimate the value of the mass transfer coefficient, K_La. The differences among many of these procedures arise from the use of a variety of values for C^*, the oxygen saturation value. Some methods assume values of C^*, others use measured or observed values, and still others allow direct estimation of C^* from the experimental data.

The term "model" is often used to describe the different methods of analysis (i.e., surface saturation model, mid-depth model, exponential model, etc). This is unfortunate because nearly all the methods use the same model to describe the transfer rate process. That model is Equation 1. As stated at the beginning of this paper, the different methods arise from two general sources. The first is the desire to analyze the data in a linear form; thus the ln deficit methods and direct methods. The second is from the modeller's ability or inability to relate the observed equilibrium oxygen concentration, C^*_∞, to the oxygen saturation value; thus the surface saturation, mid-depth, and measured C^* methods.

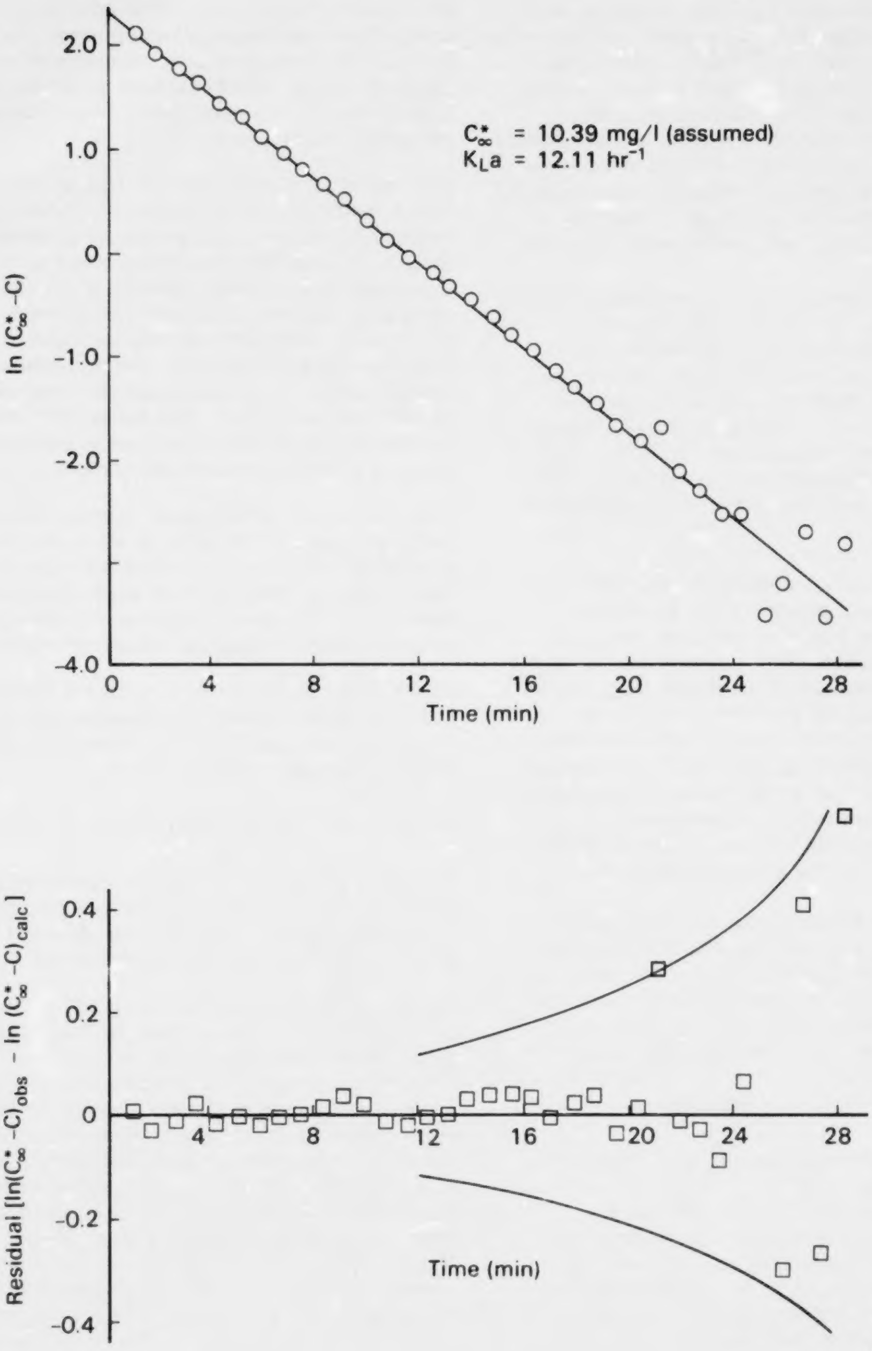
A general review of the data analysis methods is presented in Table 2. Each method is categorized according to a number of criteria for each comparison. Each will be considered briefly.

Surface Saturation Method. This method fits Equation 2 to the data. The value of C^* is assumed to be the surface saturation value (corrected for the temperature and pressure of the test conditions). The ln deficits are

Table 2. Summary of Data Analysis Techniques — Non-Steady State Oxygen Transfer Tests

Method	Equation	C^*	Truncation	Error Structure	Remarks	References
Surface Saturation	2	Surface Saturation	Usually	Biased	Inadequate for Sub-Surface Aeration	13, 19
Corrected Saturation	2	Mid-depth Effective depth	Usually	OK, except as $C \rightarrow C^*$	K_La sensitive to assumed C^*	1, 7, 14, 17, 19, 21, 23
Measured Saturation	2	C^*_∞ from data	Only if data are noisy	OK, except as $C \rightarrow C^*$	C^* and K_La determined from data	10, 11, 12, 14, 19, 20
Trial and Error	2	Trial and Error	Usually	OK, except as $C \rightarrow C^*$	C^* and K_La determined from data	2, 19
Direct Method	1	Estimated in Analysis	Usually	Errors increase as $\Delta C/\Delta t$ increases	Magnifies noise in data	10, 11, 19 22, 23
Exponential	3	Estimated in Analysis	No	Errors decrease as C increases	C_o, K_La, C^* determined from data	6, 10, 11, 12
Time Constant	3	C^*_∞ from data	Yes	?	Use limited, data sensitive to C_o	10, 19

Figure 2. Data of Gilbert and Chen (10) — Equation 2 Measured Saturation



computed and plotted vs time. Values of K_La can be obtained graphically from the slope of the line or from a numerical linear least squares analysis.

The surface saturation model has been shown to work well in describing the data from non-steady aeration tests using surface aeration devices provided there is little bubble entrainment. However, for sub-surface aeration, the method is inadequate. The observed equilibrium values are much larger than the assumed surface saturation values. As a result, estimates of K_La are too large (6) (19) and the plotted data are noticeably non-linear (10). In addition, the data must be truncated at high values of C because the computed deficits are often negative.

Corrected Saturation Method. The corrected saturation method also fits Equation 2 to the data. As in the surface saturation method, the value of C^* is assumed but in addition it contains corrections for depth and exit gas composition. The mid-depth correction (17), log mean driving force (19) (22), and effective depth (19) methods are examples of the techniques used. Once the value of C^* is computed, the ln deficits are plotted versus time and estimates of K_La are obtained either graphically or numerically.

The corrected saturation methods have met with moderate success. The basic problem is an occasional non-linear trend in the plot of ln deficit vs time at low deficits. This trend indicates that the assumed C^* is not the same as the equilibrium concentration, C^*_∞, of the observed data. To avoid the problem of non-linear plots (and negative deficits), many investigators recommend truncation of the data at 70 to 90% of C^* (13) (19) (20) (23). As shown by Boyle *et al.* (6), this is a questionable practice. Truncation can introduce considerable error in estimating K_La. The data will appear linear, but the slope will be sensitive to the assumed value of C^*.

Measured Saturation Method. Like the surface saturation and corrected saturation methods, the measured saturation fits Equation 2 to the data. In this case, the assumed value of C^* is the experimentally measured value of C^*_∞ when the aeration test is allowed to proceed to equilibrium. The ln deficits are then computed, plotted vs time, and the K a values obtained by either graphical or numerical techniques.

This method has been employed successfully by a number of investigators (10) (11) (12) (20). One of its advantages is that it recognizes the fact that the asymptomic value of the measured concentration is C^*_∞ and that from a data analysis point of view, C^*_∞ is probably the best estimate of C^* to use in Equation 2. Secondly, the plot of ln deficit vs time is usually linear when C^*_∞ is used to calculate the deficits. The main disadvantage of this method is the need to truncate the data at about 95% of C^*_∞ because positive errors in measuring C beyond that point may result in negative deficits which cannot be handled logarithmically.

The previous example where Equation 2 was fit to the data of Gilbert and Chen (10) is essentially an analysis using the measured saturation method. As was observed in Figure 2, there are statistical problems in the error structure as the deficits become small, i.e., as C approaches C^*_∞. Truncating the data at about 95% of C^*_∞ will avoid this problem.

Trial and Error Deficit Method. This is another of the ln deficit methods that fits Equation 2 to the data. It employs a trial and error procedure to determine the value of C^* that best fits the observed data. A series of C^* values are assumed. For each C^*, the ln deficits are computed, Equation 2 is fitted using linear least squares (ln deficit vs time), and the residual sum of squares Equation 4 is calculated. The fitting procedure and residual sums of squares calculation are then repeated for the next value of C^*. The value of C^* that gives the smallest residual sum of squares is selected as the best value of C^* and is used to estimate K_La.

Like the measured saturation method, this data analysis technique attempts to select a value of C^* that is compatible with the data, rather than using an assumed value based on theoretical or physical arguments. It also suffers from the same problems of truncation and error structure as the measured saturation method.

Direct Method. The direct method fits Equation 1 to the data. The slopes between successive points, $\Delta C/\Delta t$, are calculated and plotted vs the mean value of the adjacent concentrations, C. Thus:

(5) $\Delta C/\Delta t = (C_{i+1}-C_i)/(t_{i+1}-t_i)$ and $\bar{C} = (C_i + C_{i+1})/2$

The slope of the line is K_La and can be estimated using graphical or numerical procedures. Using the direct method, Equation 1 was fit to the data of Gilbert and Chen (10) and the results are presented in Figure 3.

The direct method has the advantage of not requiring that a value of C^* be specified. Rather its value is estimated from the intercept on the abscissa. This feature is attractive because of the sensitivity of K_La to the assumed C^* values in the ln deficit methods using Equation 2. The main problem with the direct method is that it magnifies the noise in the data. The process of approximating the rate of transfer, dC/dt, by taking differences in successive concentration values results in a variable, $\Delta C/\Delta t$, that has a substantially larger error than C itself. Thus, the plot of $\Delta C/\Delta t$ vs C tends to have considerable scatter, resulting in imprecise estimates of K_La. Even if the original data are smooth, the scatter in the direct method of analysis is noticeable (10). The scatter is especially disturbing if the concentration data are noisy (23). In this case it is often necessary to discard substantial portions of the data at both low and high values of C (22).

Inspection of Figure 3 clearly demonstrates these effects. The observed values of $\Delta C/\Delta t$ are scattered about the fitted curve, much more so than in Figures 1 and 2. In addition, the residuals do not appear to have uniform variance. They are large when the value of the transfer rate is large and small when the transfer rate is small. This pattern suggests that a weighted least squares procedure should be used in fitting Equation 1 to non-steady state oxygen transfer data.

Exponential Method. The exponential method fits Equation 3 to the experimental data. The values of dissolved oxygen concentration are used directly and Equation 3 is fit to the data using non-linear least squares procedures. Boyle *et al* (6) use the method of Marquardt (15) while Gilbert and Libby (11) use methods proposed by Reed and Theriault (18) in the analysis of BOD data.

One advantage of this method is that is is not necessary to truncate the data near C* because ln deficits are not computed. Secondly, the computational procedure provides least squares estimates of both C* and K_La, thus there is no need to assume a value of C* to compute K_La. In light of the sensitivity of data truncation and the assumed C* value on K_La, these are desirable aspects of the method.

The main disadvantage of the exponential method is that the error structure in C is not uniform, but is larger for low values of C than for high values (opposite of ln deficit methods). The fit of Equation 3 to the data of Gilbert and Chen (10) is an example of the exponential method. The error structure problem was discussed there and can be remedied by appropriate weighting procedures.

Time Constant Method. This method utilizes the exponential nature of Equation 3 to estimate K_La. It recognizes the fact that at integer values of the time constant product (K_Lat = 1,2,3), the values of C are respectively 63, 86, and 95% of C*. The times at which these values of C occur are estimated from a smooth plot of C vs time, and values of K_La are computed and averaged. Thus:

$$K_La = 1/t_{0.63} = 2/t_{0.86} = 3/t_{0.95} \qquad (6)$$

The method is rapid and does not require statistical treatment of data. It does require that t = 0 at C = 0, a time which is difficut to estimate precisely. Furthermore, the method uses only a limited number of data values to estimate K_La, rather than the data as a whole. It has not gained wide acceptance.

Observations on Data Analysis

The various methods of non-steady state oxygen transfer data analysis have been summarized. A brief evaluation of the procedures used to manipulate the data for statistical analysis follows.

Truncation. The ln deficit methods require data truncation as C approaches C* in order to avoid negative deficits which cannot be handled logarithmically. Truncation is necessary in the direct method because of the difficulty in estimating $\Delta C/\Delta t$ as the rate of transfer approaches zero. Boyle *et al* (6) have shown that trunction increases the correlation between K_La and C* and that it reduces the precision of the estimated K_La. Thus, although necessary with these methods (especially if the data are noisy), it is not statistically desirable. The exponential method does not require truncation.

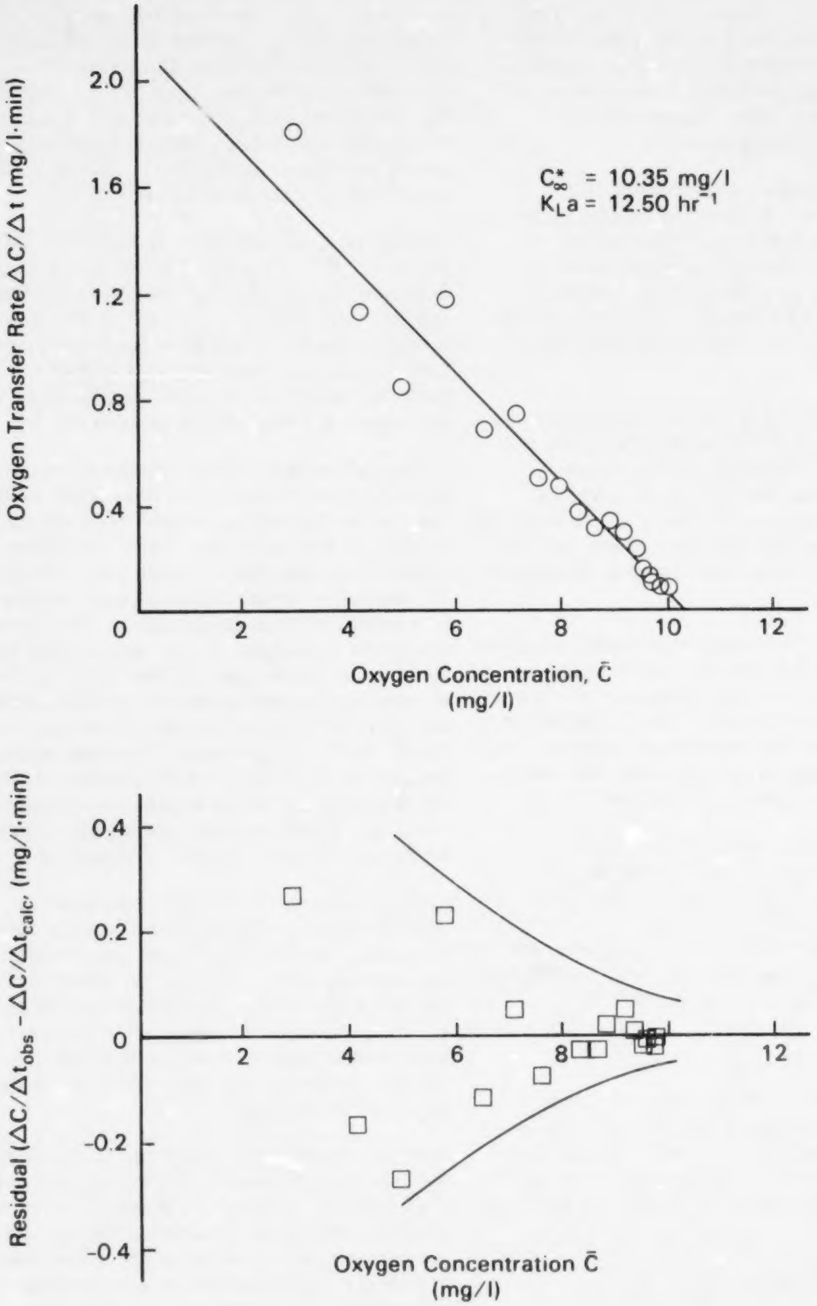
Saturation Concentration. All of the ln deficit methods require that a value of C* be assumed in order to compute the deficits. Assuming an incorrect value of C* will bias the estimate of K_La. The direct and exponential methods avoid this problem by providing estimates of both K_La and C* from the data analysis. It is important that the distinction between data analysis and data interpretation be made with respect to C*.

Statistical analysis of the data says that the value of C* should be the estimated or measured value of C^*_∞ as the test is allowed to proceed to equilibrium. This means that C^*_∞ is the value that "best" (in a least squares sense) describes the observed data. The problem of what C^*_∞ means in terms of the physical system under study is a matter of data interpretation. This is an issue that should be addressed by the mechanistic model one uses to describe the oxygen transfer process. The mass transfer model should define how C^*_∞ relates to the values of C* as they vary with time and depth over the duration of the experiment. The data analysis should be evaluated on the basis of its ability to estimate the parameters that can be estimated from these data. The direct and exponential methods are attractive in this respect because they both provide estimates of C^*_∞.

Error Structure. The ln deficit, exponential, and direct methods have problems with the error structure of the dependent variable. In Figure 2, the ln deficit method has increasingly large errors as the deficit decreases. The exponential method has increasingly large errors for low values of concentration shown in Figure 1. The direct method has overall higher errors than the other methods, and the magnitude of the error increases as the transfer rate increases in Figure 3.

For the ln deficit methods error structure can be improved by truncation. However, the truncation must be minimal if the precision of the K_La estimate is not to be compromised. Certainly data should be carried to 90% of saturation and preferably to 95%. The determining factor will be the precision with which C can be measured. A guideline for this might be to use data to within three standard deviations of C*. For example, if the standard

Figure 3. Data of Gilbert and Chen (10) — Equation 1 Direct Method



deviation of measuring C is 0.1 mg/l, then the data should be truncated when C comes within 0.3 mg/l of C*. However, the problem of determining C* remains.

Consider applying this strategy to Equation 2 and the data of Gilbert and Chen (10) considered previously. The data were truncated at C = 10.0 mg/l or 0.4 mg/l below C^*_∞. Equation 2 was fit to the ln deficits and the results with residuals are plotted in Figure 4. Clearly, the equation adequately describes the data and the residuals are well behaved. This method requires *a priori* knowledge of C^*_∞. The value used was 10.39 mg/l which was obtained from the non-linear least squares fit of Equation 3 in Figure 1.

For the exponential method, the error structure can be improved by transforming the data. In Figure 1, it is seen that as C increases, the error in C decreases. Thus, an error structure proportional to the inverse of C seems appropriate. According to Davies (8), the required transformation is to square the observed data. Thus, the square of Equation 3 should be fit to the square of the concentration values. The parameter estimation procedure still involves non-linear least squares but can be managed easily using the methods of Boyle *et al* (6).

Applying this strategy to the data of Gilbert and Chen (10) results in the fit shown in Figure 5. Note that the equation describes the transformed data adequately and that the residuals are well behaved. No *a priori* assumptions about C* are necessary with this method.

For the direct method, the errors in the transfer rate were observed to increase as the transfer rate increased. The suggested transformation (8) is to take the logarithm of Equation 1 and fit it to the logarithm of the observed transfer rates, $\Delta C/\Delta t$. Applying this strategy to the data of Gilbert and Chen (10) results in the fit shown in Figure 6. The fitted curve is fairly well defined, although there is marked scatter in the data. Examination of the weighted residuals shows that they tend to increase as the transfer rate decreases. Thus, the logarithmic transformation may have resulted in overweighting the low transfer rate values. Determination of the proper weighting procedure for these data requires further investigation. It is important to note also that this weighting technique requires that negative transfer rates (from noisy data) be discarded because they cannot be handled logarithmically.

Precision of Parameter Estimates. Equally important as the value of the estimated parameter is the precision of that parameter estimate. The precision of a parameter estimate is usually given by its variance or standard deviation (standard error), and techniques for computing these values are available (3) (8) (9) (16) (24). For the methods and data used in this paper, the precision of the K_La and C^*_∞ estimates are given in Table 3.

Inspection of Table 3 shows a number of significant points. First, C^*_∞ is estimated more precisely than is K_La. This is true whether the exponential or direct method is used, whether the analysis is weighted, and whether the data are truncated. The error in C^*_∞ for the deficit method is zero because in that method it is assumed to be a known constant. Second, the exponential and ln deficit methods provide estimates of K_La

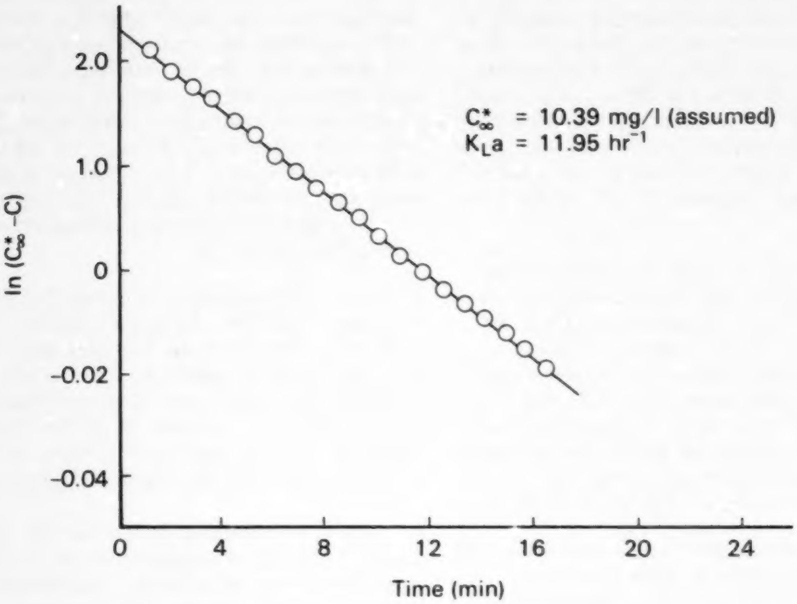
Table 3. Precision of Parameter Estimates — Data of Gilbert and Chen (10)

Method of Analysis	K_La (a) (hr^{-1})	Standard (a) Error %	C^*_∞ (b) (mg/l)	Standard (b) Error (%)	Dependent Variable
Exponential	12.05	0.9	10.39	0.1	C
Exponential (Weighted)	11.99	0.8	10.39	0.1	C^2
ln Deficit	12.11	2.1	10.39	0	$\ln(C^*_\infty - C)$
Direct	12.50	4.0	10.35	0.9	$\Delta C/\Delta t$
Direct (Weighted)	10.55	9.0	10.52	0.4	$\ln(\Delta C/\Delta t)$
Truncated Data of Gilbert and Chen (96% of C^*_∞)					
Exponential	12.13	1.9	10.37	0.5	C
Exponential (Weighted)	12.02	1.7	10.38	0.3	C^2
ln Deficit	12.13	0.4	10.39	0	$\ln(C^*_\infty - C)$
Direct	12.67	6.6	10.29	2.0	$\Delta C/\Delta t$
Direct (Weighted)	11.98	5.3	10.38	0.5	$\ln(\Delta C/\Delta t)$

(a) Least squares estimate.

(b) Relative standard deviation.

Figure 4. Truncated Data of Gilbert and Chen (10) — Equation 2 Measured Saturation



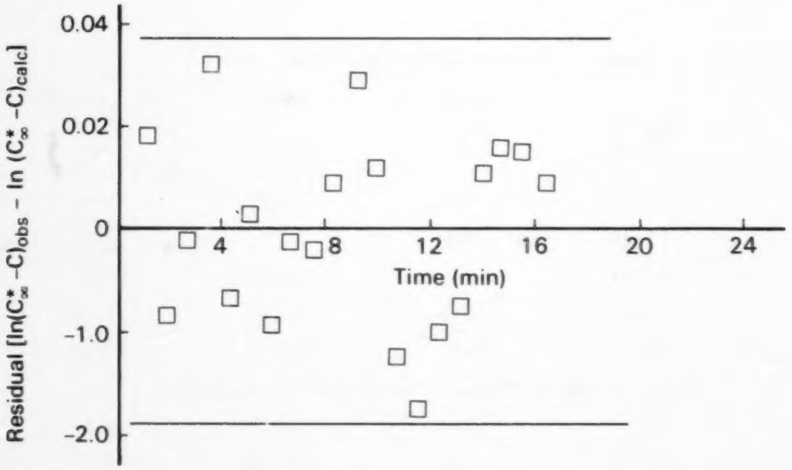


Figure 5. Data of Gilbert and Chen (10) — Equation 3 Exponential Method (transformed)

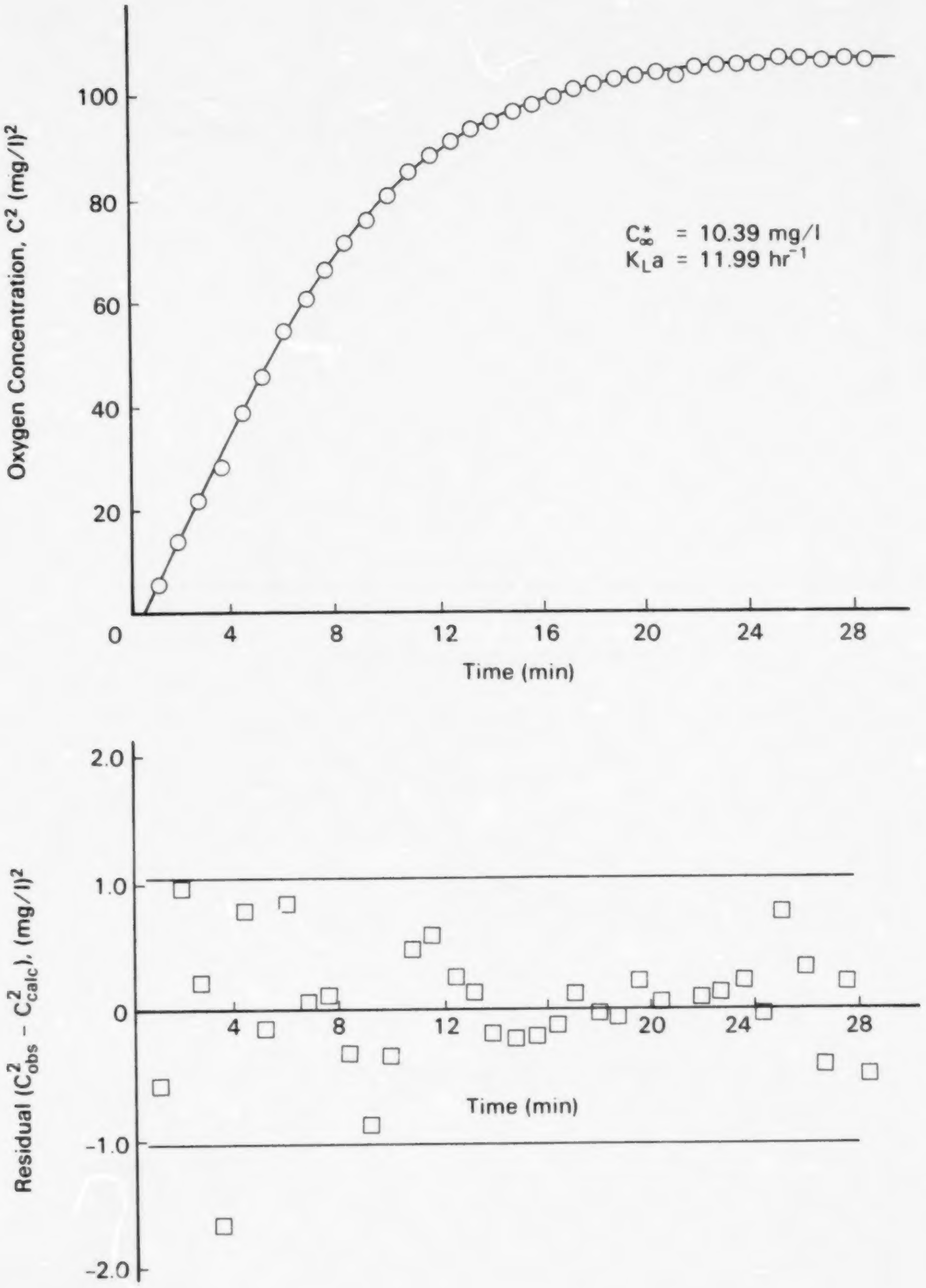
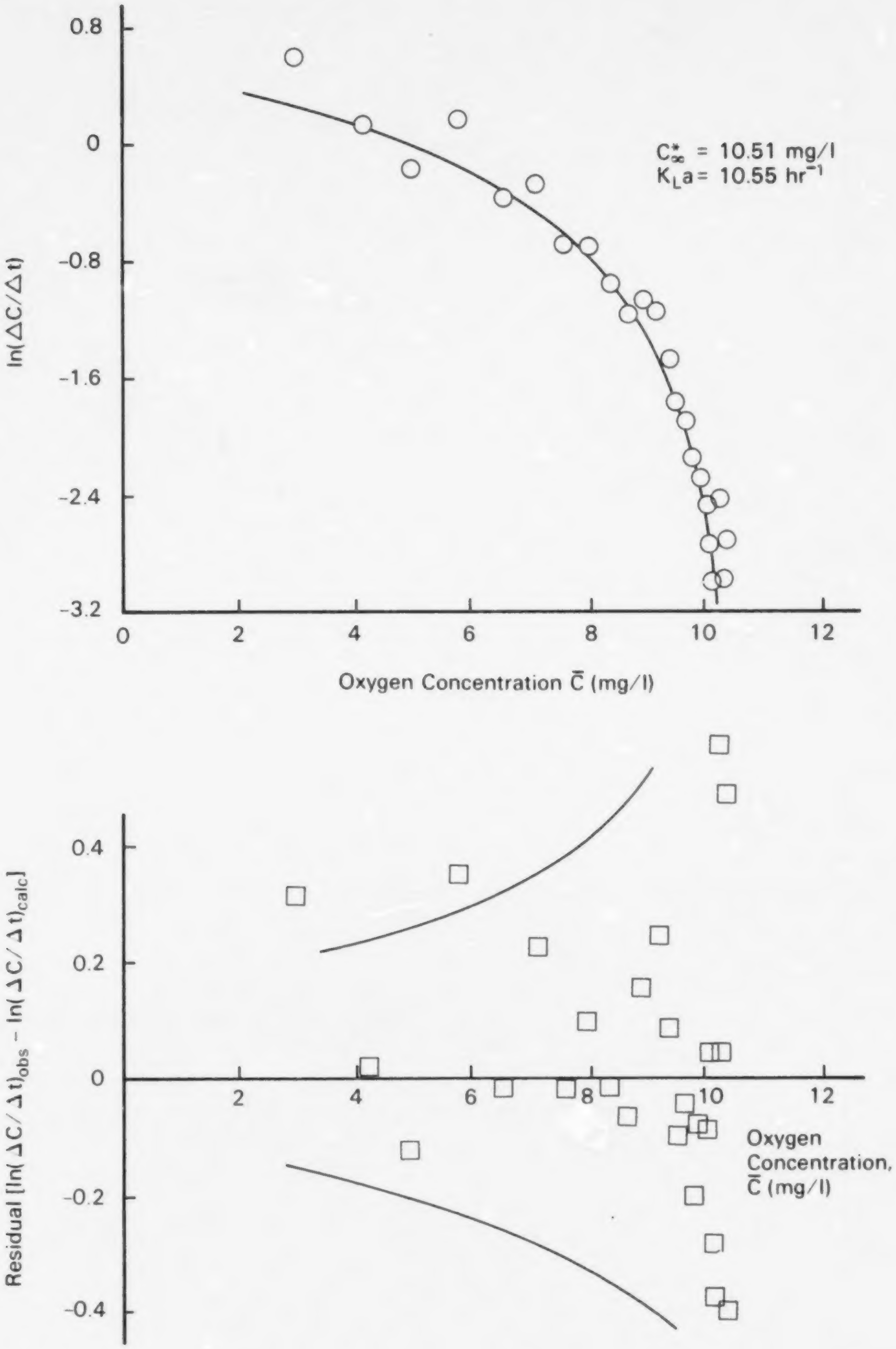


Figure 6. Data of Gilbert and Chen (10) — Equation 1 Direct Method (transformed)



having errors of 2% or less, while with the direct method, the error in K_La is from 4 to 9%. This is numerical confirmation of the larger error inherent in the direct method analysis.

Third, truncation of the data (in this case at 96% of C_∞^*) leads to larger errors in the estimates of both K_La and C_∞^*. In the case of the exponential method, the error in K_La approximately doubles, while with the direct method it increases by about 50%. The error in K_La for the In deficit method appears to decrease markedly with truncation. This apparent reduction is dependent however on *a priori* knowledge of C_∞^* and is caused by discarding the portion of the data record with the highest error.

Fourth, the weighting procedures for the exponential method and the direct method (truncated data) appear to work because they provide estimates of K_La and C_∞^* with greater precision than the non-weighted methods. The poorer performance of the weighted direct method on the entire data record is unexplained. Finally, of all the methods employed, the exponential method provides estimates of K_La *and* C_∞^* with the greatest precision (smallest error).

Summary

This paper has reviewed some of the fundamental statistical concepts in regression analysis and a variety of the analysis methods for non-steady state oxygen transfer data. The methods have been evaluated on the basis of data truncation, saturation concentration, and error structure. The exponential method, using a weighted non-linear regression procedure, appears most attractive for the analysis of non-steady state oxygen transfer data. The In direct method, using a measured saturation concentration, may be useful for approximate solutions. The direct method has larger inherent error.

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Discussion

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It was not the intent of Stukenberg, *et al*, (Ref. 23 in the paper) to recommend truncation of data from aerator testing, but merely to point out what appeared to be accepted practice by many aeration equipment manufacturers and design engineers. We recommend and perform all analyses with all the data collected without truncation or exclusion of any points.

Closure

Linfield C. Brown

Performing data analysis with all the data collected and without truncation or exclusion of any points is usually a good practice. However, in conducting the aeration test from which the data are obtained, care must be given to the following. Oxygen concentration values taken early in the test should not be affected by residual sulfite in the water. The test should be conducted and data recorded to within a few percent of equilibrium.

Review of Test Procedures

9

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Introduction

The purpose of this paper as set forth in the Workshop scope statement is to review and summarize the non-steady and steady state test procedures cited in the literature as well as those currently in use by owners, consultants and manufacturers. Emphasis should be placed on the significant differences between the various procedures. The sessions following this paper will address many of the areas of testing procedure in more detail. The primary focus of this paper is on the methodology of obtaining the necessary data for oxygen transfer rate analysis. Most of the discussion deals with clean water testing as it is and has been the most widely practiced procedure.

There is a considerable amount of literature, and many approaches are currently in use in testing. There is no commonly accepted procedure, and engineering specifications outlining test procedures vary considerably. It can be observed that there is a real need for early communication between owner, engineer and manufacturer in developing appropriate, well-described test procedures in specifications. Several of the more frequently cited references on testing procedures have been utilized in preparing this paper.

The following test procedures will be discussed:

Non-Steady State Clean Water — Sulfite Deoxygenation or Nitrogen Stripping

Non-Steady State Clean Water Plus Surfactant

Steady State Mixed Liquor

Some observations on evaluation of mixing will also be noted.

Air Flow and Power Level

Accurate measurement of air flow rate is very critical to reliable testing results. This is more difficult in field testing due to the fact that the full-scale air flow measurement system may not be applicable and/or sufficiently accurate for a test in one of several aeration tanks. It would be helpful for design engineers to consider this question when anticipating equipment evaluation. In large plants, it may be desirable to more fully instrument (air flow measurement, sampling system) one of the tanks for use by the owner in performance and operational studies.

Several methods of air flow measurement are being used including the plant system, orifice plates, pitot traverses, Annubar units and other devices. It is important to obtain proper calibration and equations for air flow where appropriate. System temperature and pressure are measured to adjust the flow rate to standard conditions. The barometric pressure and relative humidity are also observed.

Some testing requirements include measurement of system or diffuser head loss. Pressure taps and manometers are commonly used. The data are used in meeting specifications and for calculating horsepower requirements by the adiabatic compression or polytropic formulae approach for diffused air systems.

Mechanical aeration units require various measurements depending on the type of system. Factors such as speed and immersion are observed. Power measurements are made using watt meters, strain gauges, torque pick-ups and other appropriate equipment for the type of unit.

Physical Requirements — Geometry and Power Level

Eckenfelder (8), Morgan and Bewtra (18) and Bewtra and Nicholas (4) have reported on the importance of the test tank geometry. It is generally recognized that tank geometry, aerator placement and power level affect oxygen transfer performance. Normally one attempts to closely simulate field installation conditions for specific applications. However, it would be desirable to have an acceptable geometry for comparisons of aerator performance.

Berk *et al* (3) of the Wastewater Equipment Council, Process Equipment Manufacturers' Association (PEMA) recommended some criteria in 1972. They are:

Mechanical Aeration

Tank Depth: 8 to 15 ft (as low as 3 ft for horizontal devices)

Power Level: 0.04 to 0.3 hp/1000 gal

Note: Higher power levels and/or deeper basins will be permitted where the aeration device is being tested for application in specific waste treatment systems which would operate under similar or identical conditions.

Diffused Aeration

Basin Size: A rectangular tank with minimum length = 1.5 x depth, tank width = 3 to 6 ft

Air Supply: 12 to 45 scfm/1000 ft^3

Diffuser Submergence: 8 to 14 ft

Note: Higher air supply and/or more diffuser submergence and/or smaller tank length and width will be permitted under test conditions where the aeration device is being tested for application in specific waste treatment systems which would operate under similar or identical conditions.

The Oxygen Transfer section of Standard Methods for the Examination of Water and Wastewater (2) notes that the value of K_La is affected by the type of aeration equipment and tank geometry. It is stated that "there are no universally accepted factors that can be applied to all aerators and test conditions to establish a reproducible K_La." It is observed that the power applied to a unit volume ratio is widely used to compare one system to another. An acceptable range of 0.05 to 0.20 hp/1000 gal is cited.

Test Tank Preparation

Standard Methods for the Examination of Water and Wastewater (2) states that the aeration basin should be cleaned thoroughly and filled with fresh tap water. Berk *et al* (3), Water Pollution Control Federation Manual of Practice No. 5 (25) and Mixing Equipment Co. (16) also recommend cleaning. One concern is to avoid any residual surfactant or other chemical effects on the testing. Several references recommend maintaining the temperature of the fresh water at about 20°C, if possible.

Water Quality

One is normally constrained by whatever water supply is available. Most procedures do not prescribe an overall acceptable water chemistry. Cobalt and total dissolved solids (TDS) or sulfate concentrations are commonly cited. Some water quality parameters of concern that have been noted are as follows:

Parameter	Comment
Temperature	DO Concentration, K_La
Cobalt	Adequate Catalyst. Affect on Winkler DO Measurement
Total Dissolved Solids (Na_2SO_4)	Affect on Winkler DO and K_La Testing Limitation
pH, Hardness (Alkalinity), Iron, Manganese	Cobalt Interactions and Effect on Winkler DO
Dissolved Gases	Probe Readings

Scaccia and Lee (21) have cited various Winkler DO interferences including iron salts, residual chlorine, inorganics, etc. They recommend the use of a chemical blank to account for these interferences. It must be recognized that source water quality varies widely and modifying testing dates or altering water quality for testing are difficult alternatives. In the Midwest, for example, water temperature may vary from 5°C to 25°C, initial TDS values may approach 1000 mg/l and it is possible to experience frothing of treated surface water supplies.

System Stability

Various references recommend aeration periods of from 20 to 30 min to achieve a steady state mixing condition in the test basin. Landberg *et al* (13) observed that a 75-hp aerator requires 30 to 40 min to achieve a steady state hydraulic regime at the Lightnin test facility. They note that power readings and mixing velocities can be monitored to determine steady state. They observed that steady state power readings are achieved before steady state hydraulic conditions result. It was noted that each test condition (aerators and basin) will have its own time constant.

Standard Methods for the Examination of Water and Wastewater (2) notes that the aeration device should be able to be operated at a constant power output for the duration of the test. Stukenberg *et al* (24) have reported on difficulties and procedures in designing shop tests to simulate power levels of field-scale surface aerators.

Berk *et al* (3) state that the air temperature during the test period should not be in excess of 10°C above or below the temperature of the test tank water.

Various references note that the water level should be maintained at its proper elevation and flows that are withdrawn and overflowed should return to the test basin.

Deoxygenation

Nitrogen stripping and sodium sulfite are the two methods employed. Nitrogen stripping is primarily used in laboratory and some shop testing. Pentech Industries has utilized nitrogen stripping in shop testing. They have observed that there is an initial lag period in the uptake curve after returning the system to aeration.

Sodium sulfite has been supplied from various sources in testing programs. One of the concerns with the chemical source is the amount of impurities present that may interfere with the test. Sodium sulfite is commonly used in power plants for deoxygenating water, and some sources contain catalysts. NALCO 19 is one example. It contains cobalt. Multiple additions of NALCO 19 to a test tank would result in an increasing cobalt concentration.

Santosite (Monsanto Chemical Co.) is a commonly used source of sodium sulfite. It has generally been found to be free of cobalt. Scaccia and Lee (21) have reported on "contaminated" Santosite. They have found that some batches of the chemical yield significantly different K_La results for identical test conditions. They have recommended conducting a laboratory performance test on each batch of sulfite and comparing it to performance using nitrogen stripping. One could identify acceptable sulfite chemicals in this manner.

There are other suppliers of sodium sulfite including Stauffer and Allied Chemical. To avoid the potential impurities problem, a reagent grade or high quality sodium sulfite could be used. The cost differential is significant for large-scale testing. Present Midwest costs for reagent grade sulfite are approximately $0.85/lb while Santosite costs approximately $0.25/lb. For a test at 10 mg/l DO concentration with a 50% excess sulfite addition, you would need approximately 1 lb/1000 gal test volume.

Cobalt is provided either as cobalt chloride or cobalt sulfate. A wide range of cobalt concentrations have been and are being used in testing procedures. One concern is to have a high enough concentration to be certain to catalyze all of the sodium sulfite added. Morgan and Bewtra (18) have reported on the effect of cobalt concentration on the rate of sulfite depletion. Mixing Equipment Company (16) recommends a concentration of at least 2.0 mg/l. Berk *et al* (3) cite an upper limit of 2.0 mg/l. The second concern is the interaction of cobalt with the Winkler DO determination. Kalinske *et al* (11) have reported that cobalt concentrations in excess of 0.05 mg/l interfere with the Winkler DO test. Several references are recommending concentrations in the 0.05 to 0.10 mg/l range. The Los Angeles County Sanitation Districts' testing program as reported by Yunt (26) is using 0.10 mg/l of cobalt.

Scaccia and Lee (21) reported that cobalt concentrations in the range of 0.5 to 1.5 mg/l had no effect on test results and they normally use 0.5 mg/l. Stanton and Bradley (23) recommend the use of 2.0 mg/l to assure adequate catalyst and the use of chemical test modifications to account for any Winkler DO test uncertanties.

Sulfite Addition

The reaction for sulfite deoxygenation is as noted below:

$$Na_2SO_3 + (1/2)O_2 \xrightarrow{Co} Na_2SO_4$$

The theoretical requirement is 7.88 mg/l of sulfite per 1.0 mg/l DO concentration. Sulfite additions are made in excess and are dependent on the oxygen transfer rate and mixing rates. Standard Methods for the Examination of Water and Wastewater (2) and Manual of Practice No. 5 (25) suggest 0.8 lb/1000 gal which is 22% excess. Berk *et al* (3) suggest 1 lb/1000 gal which is 50% excess. Various references cite the necessity to go to 2.0 to 2.5 times the stoichiometric value to achieve a zero DO concentration. Most procedures do not indicate any criteria regarding reaching zero DO or the length of time at zero DO during a run. Stukenberg *et al* (24) state that "if the basin is not deaerated completely, the results will be erratic and difficult to interpret."

The sulfite should be dissolved prior to addition to the test tank. Warm water increases the solubility of sodium sulfite. Saturated solutions contain 2.23 lb/gal at 20°C and 3.0 lb/gal at 30°C. Manual of Practice No. 5 (25) states that the sulfite should be released in at least two locations. It is important that the sulfite be uniformly and rapidly dispersed. Some investigators have used pump-fire hose combinations to disperse the chemical. Considerable effort is warranted to attain proper dispersion in large basin testing projects.

Sulfite Reaction and Testing Limitations

Scaccia and Lee (21) have observed interferences and lack of reproducibility with the initial test of a newly filled test tank. This has also been observed by others. They recommend pre-conditioning the water by reacting it with sodium sulfite and then aerating to saturation before starting the test program.

The following is an example of the water quality change as a result of sulfite addition experienced by the writer.

Test tank	64,000 gal
Theoretical	For each 100 lb Na_2SO_3 added, 117.5 lb of Na_2SO_4 is formed. The increase per 100 lb added is TDS-220 mg/l and SO_4-149 mg/l.

	TDS (mg/l)		SO_4 (mg/l)	
	Theo.	Actual	Theo.	Actual
Initial	—	218	—	27
After 500 lb	1320	1250	771	800

It has been common practice to limit the number of runs on a given volume of water. The total dissolved solids or sulfate concentration has been used as a basis. Landberg *et al* (13) noted an effect on the Winkler DO determination at 1.0 mg/l cobalt concentration and a Na_2SO_4 concentration of about 1200 mg/l. The following are examples of testing limitations:

Berk *et al* (4)	Na_2SO_4 Conc. of 1500 mg/l (1010 mg/l as SO_4)
Yunt (8)	Na_2SO_4 Conc. of 2000 mg/l
Scaccia and Lee (4)	SO_4 Conc. of 1000 mg/l

Procedural references do not make any distinction on upper limits if probe analysis for DO concentration is utilized. Some testing has been conducted at higher TDS concentrations without apparent reproducibility problems. The allowable TDS level could significantly limit the number of acceptable tests for each tank filling on waters with high naturally occuring TDS levels.

Sampling Locations

Standard Methods for the Examination of Water and Wastewater (2) and Manual of Practice No. 5 (25) do not specify the number or location of sampling stations. They indicate that it is based on the type of device, size and geometry of the test tank. Smart of the Ontario Ministry of the Environment (22) recommends a minimum of three locations. Mixing Equipment Company (16) recommends four sampling points. Miller (15) has shown that a sampling system that composites samples from five locations at mid-depth results in a sample that represents the tank contents for diffused air systems. Berk *et al* (3) recommend four sample points at two locations. Location 1 would be close to the device at mid- and 3/4-depth. Location 2 would be near the tank periphery at 1/4- and 3/4-depth. Yunt (21) is using four sample points at two locations. Two samples are from mid-depth, one from 2 ft below the surface and one from 2 ft above the tank floor.

Stukenberg *et al* (24) recommend a minimum of three sample points. They note that five to seven points is normal with more than ten points utilized for abnormal situations. The following sketch from Stukenberg *et al* (24) illustrates recommended sample point locations for three aeration systems.

The procedures cited in the references attempt to obtain both a vertical and horizontal representation of the DO or oxygen uptake. They also consider locations that will represent regions of maximum variation in DO or oxygen uptake.

Sampling — In Situ, Transport, Timing

One approach to DO measurement and oxygen uptake measurement is to utilize in-tank DO probes, preferably equipped with stirrers. The probe readings can be read directly, recorded on multichannel recorders and/or printed with digital output systems. This procedure yields the largest amount of data and in some cases is a continuous DO record for the duration of the run.

Several references recommend either gravity or pumped samples. Standard Methods for the Examination of Water and Wastewater (2) recommends the use of submersible pumps equipped with anti-air entrainment inlets. The sampling interval should depend on the power level with at least six sets of samples obtained between 10 and 70% saturation. DO analysis can be by probe or Winkler procedure.

Manual of Practice No. 5 (25) recommends the use of pumps with a sampling interval of 1 to 2 min and the collection of six sets of samples between 10 and 70% saturation. DO analysis by the Winkler procedure is recommended.

Berk *et al* (3) recommend the use of pumps or siphons with the BOD bottle volume displaced at least three times and preferably ten times between samples. The collection of at least six sets of samples between 10 and 80% saturation using an in-tank DO probe to set the timing of samples is recommended along with DO analysis by the Winkler Method.

Berk *et al* (3) and Standard Methods for the Examination of Water and Wastewater (2) present an equation for determining the sampling interval.

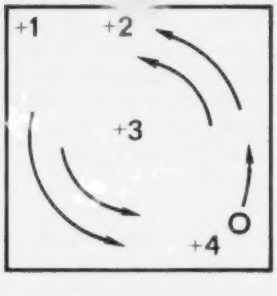
$$t = (138\ W/OC) \times 1.024^{(20-T)}$$

where: t = sampling interval, min
W = million lb of water in test tank
OC = oxygenation capacity expected, lb/hr
138 = constant for 80% saturation termination
T = temperature, °C

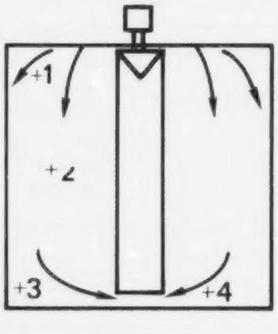
Testing with rapid oxygen uptake systems require sampling intervals of less than 1 min.

Sanitaire (20) recommends the use of rotameters or other flow measurement methods to monitor the pumpage rate. They recommend a maximum residence time in the piping of 20 sec. Using DO probes to set intervals, they obtain a minimum of ten samples per location with eight taken between 20 and 90% saturation. The BOD bottles should have two displacements and the DO analyzed by Winkler titration. (Note. A recent procedural change recommends the use of *in situ* probes.)

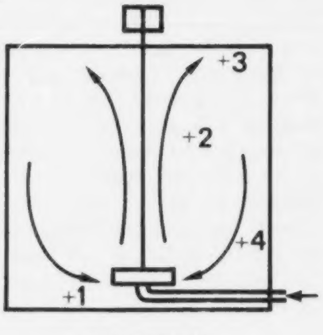
Yunt (26) is using submersible pumps with a continuous BOD bottle flow at five bottle displacements in 15 sec. A



Diffused
Aeration



Surface
Aeration



Submerged
Turbine
Aeration

minimum of eight samples are taken between 20 and 80% saturation. Initially, DO analysis was by probes. However, a recent change to Winkler analysis was made.

Ewing *et al* (9) observed that higher K_La values were observed with *in situ* probes versus piped Winkler samples. The writer has observed a similar response with high oxygen uptake rate systems and slow BOD bottle filling rates.

An alternate approach used by the writer and others in some studies is to mount a DO probe in the sampling line. A uniform velocity past the probe is obtained. Dissolved oxygen would be recorded continuously and discrete samples could be taken for simultaneous Winkler analysis. The DO response curve parallels the in-tank probe response with a time delay equal to the sample transport time.

Conway and Kumke (6) continuously pumped a sample into a BOD bottle equipped with a magnetic stirrer. A probe was used to determine the DO concentration.

Dissolved Oxygen Analysis

The Winkler titration method is widely recommended as the DO analytical procedure. Kalinske *et al* (11) have reported on a cobalt interference resulting in higher DO readings as the cobalt concentration increases. The limiting concentration for no interference is 0.05 mg/l. Lakin (12) and Stanton and Bradley (23) have recommended a testing modification to correct for chemical interference.

They recommend titrating a sample without the addition of $MnSO_4$. This result would be subtracted from every DO measurement to eliminate chemical interferences. Scacci and Lee (21) also recommend the use of a chemical blank to account for chemical interferences with the Winkler analysis.

Montgomery (17) reports that the loss of iodine vapor in titrations may result in lower DO values especially at the higher concentrations. Procedures using excess iodide to prevent this loss are cited.

Mixco (16) and Conway and Kumke (6) recommend that care be taken to keep the BOD bottles at or below the temperature of the basin.

DO probes are used in many testing programs. In some cases, the probes provide primary data and in others they are used to control Winkler sampling procedures or provide supplemental information. Standard Methods for the Examination of Water and Wastewater (2) reports an accuracy of ±0.10 mg/l and a precision of ±0.05 mg/l for probe DO analysis.

Probes have been successfully used; however, many references cite concerns regarding special care, extensive calibration requirements, sensitivity and response delays. Gilbert and Chen (10) report on the successful use of DO probes with a multi-channel recording system. Probes were calibrated using saturated test water. The writer has successfully utilized in-tank and externally mounted sample line probes using the test water for calibration. Results have generally compared favorably with Winkler data. Yunt (26) initially used BOD bottle probe analysis of piped samples in the Los Angeles County Sanitation Districts' testing program. The probes were calibrated using a saturated air method. Scaccia and Lee (21) cite difficulties in maintaining probes in perfect working order. They recommend an extensive procedure of calibration using distilled water.

Ewing *et al* (6) state that "there is some indication that more representative and reliable non-steady state test data may be obtained by multiple dissolved oxygen probes than by piped samples analyzed by Winkler or probes. The assumption of careful analysis and selection of the probe system, its application and calibration schedule is inherent in the above conclusion." Conway and Kumke (6) concluded in their study that reliable oxygen analyzers make possible more rapid, accurate measurements of oxygen transfer.

The utilization of probes with automatic data printing would also eliminate personal error in reading meters or strip-chart recordings. Probes also may avoid some of the chemical interference questions although the questions of calibration procedure and comparative data remain.

Run Length and Truncation

As noted earlier in the discussion of sampling intervals, several references have suggested terminating a test at from 70 to 80% of DO saturation. Boyle *et al* (5) have cited the importance of obtaining data closer to saturation to minimize errors in estimating K_La. Recent procedural approaches are more frequently recommending that DO data be taken to 90 or 95% of the DO at saturation. Boyle *et al* (5) also noted that truncation of DO data up to 20% of DO saturation will not affect the precision of the estimate of K_La.

Dissolved Oxygen Saturation

It has been common practice to utilize DO values from references such as Standard Methods for the Examination of Water and Wastewater (2) and modify them for test location and submergence conditions in the case of diffused air systems. A mid-depth or mid-submergence value has been calculated using Standard Methods for the Examination of Water and Wastewater (2) pressure corrections or the method of Oldshue which recognizes the variable concentration of oxygen in the air bubble while in transit in the tank. Modifications of these procedures have been suggested by some investigators.

Boyle *et al* (5) have illustrated the effect of utilizing incorrect DO saturation values and have compared test values with calculated values. Conway and Kumke (6) note that the careful determination of the test oxygen saturation level requires more emphasis.

Various references are recommending obtaining a test DO saturation value for a given volume of water by extending a test run until the DO concentration remains constant for a 15 min period. Stukenberg *et al* (24) note that the length of an extended run for DO saturation can be estimated by multiplying $1/K_La$ by 6. It is normally recommended that measured DO saturation values be determined whenever there are power level or geometry changes in the testing program.

Various references cite concerns regarding determining the appropriate measured saturated DO concentration for mechanical aerators with significant entrained air or sparge systems. Significant DO gradients at saturation have been noted.

Repetitive Runs and Precision

Stukenberg *et al* (24) recommend that three replicate tests should be run per set of aeration conditions. Standard Methods for the Examination of Water and Wastewater (2) notes that a single K_La value is reproducible within ±15% of the mean for multiple aerators and ±8% for a single aerator per basin. Conway and Kumke (6) report a variation of less than 5% of the average values in their testing program.

DO and K_La Variations at Sample Points

Standard Methods for the Examination of Water and Wastewater (2) observes that if the DO concentration at all points is within 0.25 mg/l of the average DO at time t, you may make one K_La determination. If not, then determine individual K_La values for each sample point. Non-parallel slopes indicate incomplete mixing, and the test results should be voided.

Smart (22) requires that the K_La values for each sample point shall not vary more than ±10% from the average value. Greater variation shall indicate incomplete mixing and invalidate the test. Other references have suggested allowable K_La variations of from ±6.0 to ±7.5%. One approach used is to accept the test if two thirds of the sample points are within ±10%.

Clean Water Plus Surfactant

Various references have reported on experiences in testing with the addition of surfactant to more closely simulate field conditions. Ewing *et al* (9) reported that the LAS concentration decreased substantially during the test and that the results were quite varied indicating poor reproducibility. Testing problems with excessive foaming were also observed. Ewing *et al* (9) note that efforts in this area are justified to improve methods for predicting actual operating oxygen transfer values.

Downing and Boon (7) have conducted an extensive testing program with anionic detergents. Upon the addition of anionic detergents, they observed a decrease in transfer efficiency with diffused aeration and an increase in K_La for several mechanical systems.

Steaty State — Mixed Liquor

Steady state testing is being utilized to evaluate oxygen transfer under actual operating conditions. Several of the testing procedural concerns involved in clean water testing also apply to the steady state method. In addition, steady state testing requires the evaluation of the biological oxygen uptake rate, the alpha factor and the beta factor.

The condition of steady state must first be achieved. It is normally defined as the condition where the biological oxygen uptake rate is constant and there is no change in DO concentration. One of the difficulties with the procedure is achieving steady state conditions and uniform data throughout the test tank. Stukenberg *et al* (24) have suggested establishing endogenous respiration conditions in the test tank to provide a more uniform uptake condition. Results of this approach are discussed in a later paper (Paper No. 21).

The oxygen uptake rate is determined at several locations. Many factors affect the reliability of measurement of this rate determination. Standard Methods for the Examination of Water and Wastewater (2) recommends transferring a mixed liquor sample to a BOD bottle and measuring the uptake with a DO probe equipped with a stirrer. Conway and Kumke (6) and others have noted the importance of conducting the oxygen uptake measurement as soon as the sample is collected to obtain reliable results. Downing and Boon (7) and others have utilized an off-gas analysis and mass rate balance to determine the uptake rate.

Albertson and DiGregorio (1) have reported that field K_La values are dependent on the biological oxygen uptake rate and increase as the biological oxygen uptake rate increases. This finding can have a significant impact when clean water K_La values are used in predicting steady state K_La values.

Alpha Factor

The alpha factor enables comparisons between K_La values in tap water and wastewater and is evaluated in steady state testing. Standard Methods for the Examination of Water and Wastewater (2) suggests using a bench-scale aerator at various mixing intensities. Stukenberg *et al* (24), Gilbert (10) and others recommend using a laboratory scale unit where the K_La in the laboratory unit is adjusted to the same value as in the full-scale tank. Ewing *et al* (9) indicated that they were unable to obtain consistent alpha values, bench-versus full-scale using the matching K_La approach. Most sources note that the alpha factor is sensitive to the type of aeration device and mixing intensity.

Beta Factor

The beta factor relates the DO saturation under test conditions to that in tapwater. Standard Methods for the Examination of Water and Wastewater (2) recommends saturating a sample by violent hand mixing in a half-full liter jar. Stukenberg *et al* (24) and others utilize bench-scale aerators. Aeration for a period of six times $1/K_La$ is suggested to achieve saturation.

Off-Gas Analysis

An alternate approach in steady state testing is to capture the off gases from the aeration tank, analyze for the oxygen content and determine the oxygen transferred by a mass balance on the system. There is significant potential for error in this approach; however, it has been used successfully in several studies including those by Downing and Boon (7), Conway and Kumke (6), Novak (19) and Leary, Ernest and Katz (14). There is some interest in using this system for pure oxygen closed system analysis.

Tracer Approach

The use of tracers to evaluate oxygen transfer is proposed in the paper to be presented by Mr. Lawrence A. Neal (Paper No. 23).

ASCE (1965) Study Conclusion

Conway and Kumke (6) studied various approaches in evaluating oxygen transfer performance and concluded that "the reoxygenation (clean water) approach probably should be used in testing to meet purchase specifications and the biological uptake approach (off-gas or oxygen uptake measurements) used for plant operating purposes."

Mixing

Specifications for evaluating aeration equipment frequently include mixing requirements. Oxygen transfer test validity is also evaluated based on mixing conditions in the test tank.

Parameters that have been used include allowable ranges of K_La variation as noted earlier, uniformity of mixed liquor suspended solids concentrations, oxygen uptake rate variations, bottom velocities and tracer dispersion evaluations.

There is some question regarding the reliability of velocity measurements in air-water mixtures with Price or Gurley cup meters. Dense total floor diffuser placements do not develop horizontal bottom velocity patterns. A velocity meter without moving parts (Marsh-McBirney) has been used by some investigators.

Stukenberg *et al* (24) note that mixing is difficult to determine and generally velocity measurements are the only check on equipment. They indicate that the inherent error in the suspended solids test minimizes the value of using uniform MLSS concentration as a criterion. They further state that a good indication of uniform mixing is either the uniformity of dO/dt throughout the basin in the steady state test or the uniformity of K_La in the non-steady state test.

Summary

It is not the intent of this summary to propose an oxygen transfer testing procedure for consideration. Several observations as to areas of procedural development and approaches that may be considered are noted. The dual concern of ability to reliably evaluate equipment for compliance and to truly predict performance under actual field conditions must be implicit in any procedural development.

1. A guideline for the extent of repetitive testing required to establish compliance should be developed. In addition, a recommended reproducibility or precision for this repetitive testing in clean water and steady state should be established.
2. Agreement should be reached on acceptable variations in K_La for the various sample points and the interpretation if one or more locations are not within this range. Should the test be invalidated or should some data points be removed from consideration in establishing a mean or composite value?
3. A recommended tank geometry for different types of aerators would be desirable for shop testing comparisons.
4. Some procedural guidelines on criteria for air flow measurement, power measurement, system stability requirements and related data should be included.
5. The required number of sample points and recommended location criteria should be included. The duration of a run and minimum number of sets of data per location should also be noted with due consideration given to truncation limitations.
6. The question of what is an acceptable water chemistry in clean water testing as well as what the upper limit for TDS concentration should be to terminate testing on a given batch of water should be resolved. Procedures for appropriate quality control on the sodium sulfite used should be included.
7. The level of cobalt to be used needs to be resolved in light of adequate catalyst requirements and chemical testing interferences.
8. The use of DO probes appears to be a desirable approach for clean water testing. They enable an essentially continuous data base when associated with recording equipment. Discrete value print-outs would be a desirable feature. Probes also are not affected by several of the water chemistry problems cited in references. Probes with stirrers would be placed at in-tank locations or possibly used in sample line mountings.

An appropriate calibration procedure will require development and acceptance. The successful use of probes and recording equipment does require considerable attention and care.

9. The Winkler method would be used in the calibration procedure, and it is suggested that at least one sample location could also be analyzed by the Winkler method. The use of the Winkler method will require resolution of the use of chemical interference blanks and other procedural uncertainties such as iodine vaporization.
10. Procedural requirements for obtaining a measured C* should be noted. Factors affecting this determination including type of aerator and power level should be considered.
11. The criteria for indicating an acceptable steady state condition should be noted. Reliable methods for evaluating biological oxygen uptake rates should be established with guidelines for acceptable variation in the sample point values. The question of variability of K_La with oxygen uptake rates should be resolved.
12. A common bench-scale procedure for alpha and beta determination should be included.
13. In that mixing requirements are associated with oxygen transfer equipment, it would be desirable if recommendations for evaluating mixing could also be developed.

There are many items to consider in developing a testing procedure as well as the additional area of data interpretation and methods of calculation. When one considers all the potential items that could or should be delineated and observes the many testing difficulties cited in the literature, the drafting of a standard can appear to be an overwhelming task. One is tempted to suggest that the standard be as follows:

"The performance of the oxygen transfer equipment will be tested for compliance in a professional manner on a sound scientific basis in consultation with the owner, engineer, contractor and manufacturer."

This Workshop definitely has a significant challenge before it in aiding in the development of an oxygen transfer standard.

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Section III. Clean Water Testing: Shop and Field

Influence of Tank Geometry on Aerator Performance

10

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Introduction

Some of the technical information included in this paper is part of a presentation to be given at the Purdue Industrial Waste Conference in May, 1978. These results have been made available to the Asilomar Workshop to aid in developing a realistic and practical oxygen transfer standard for the industry. Envirex Inc. can only capitalize on the over two million dollars invested to date in aerator testing if such a standard gains general acceptance and if the relationship between the "standard test" and process design is defined.

Envirex Inc. (Rexnord) began conducting non-steady state aeration tests on various types of aeration devices 20 years ago, and for the last 15 years, the procedure has remained basically unchanged. This standardized Envirex aeration test procedure allows a comparative analysis of the influence of tank geometry on the types of aerators tested.

Based on the findings of the Subcommittee on Oxygen Transfer Standards, this procedure may ultimately be modified; however, the order of magnitude of the defined geometry influences will likely remain unchanged.

Mechanical Surface Aerators

Types of Aerator Tested

1. High Speed — Floating units with direct-coupled, axial-flow marine style props operating at motor speeds of 900 to 1800 rpm.
2. Low Speed — Floating and bridge mounted motor/reducer drives with output speeds of 25 to 125 rpm. Most of the data were developed with the Envirex low speed impeller; however, other configurations were also examined.
3. Oxidation Ditch — Aerators consisting of a series of discs rotating on an axis mounted across the ditch. The number, speed, submergence, and configuration of the discs can be changed to influence oxygen transfer capacity and input horsepower.

Specific Tank Geometries Tested

1. High and Low Speed Aerators

Basin Dimensions	Volume (gal)
a. 16′ square x 15′ deep	29,000
b. 20′ square x 18′ deep	54,000
c. 40′ square x 20′ deep	240,000
d. 40′ wide x 80′ long x 20′ deep	480,000
e. 80′ square x 20′ deep	960,000

2. Oxidation Ditch Aerators

Length (ft)	Volume (gal)
a. 65	7,000
b. 130	75,000
c. 215	110,000
d. 285	180,000
e. 335	240,000

Influence of Geometry on Oxygen Transfer

1. Transfer rate per unit volume vs power input:

    (a) Figure 1 is a generalized example, but all surface mechanical aerators tested within the stated geometry follow the same linear relationship.

    (b) All high and low speed aerator power inputs were from single units except in the 40 ft x 80 ft and 80 ft square basins. Multiple units tested in these basins fell on the same line as total horsepower.

    (c) For each specific geometry, aerator efficiency in terms of pounds of oxygen per horsepower hour, was constant from the smallest to the largest.

    (d) By varying the depth it was possible to duplicate the horsepower/volume ratio of two different geometries using the same aerator. In all cases the actual oxygen transfer efficiency was dictated by the specific test geometry.

2. Oxygen transfer rate vs surface area of test basin:

    (a) Figure 2 is a generalized form followed by all types of surface aerators tested.

    (b) Each horsepower size tested at a specific depth reached a surface area where there was little if any

Figure 1. Oxygen Transfer Rate Per Unit Volume vs Shaft Horsepower of Mechanical Surface Aerators

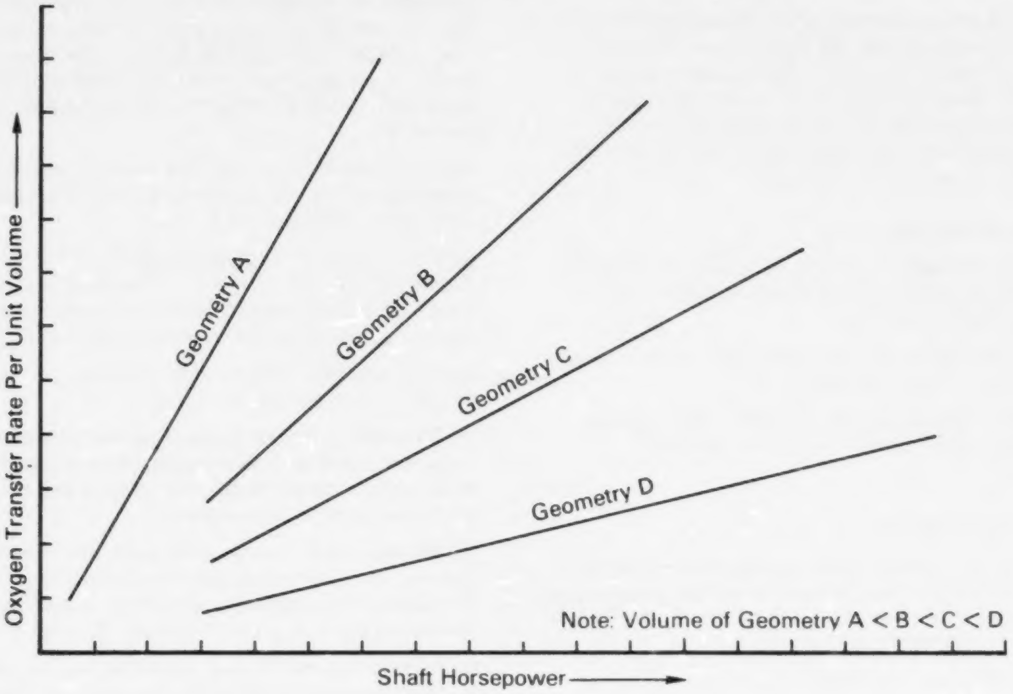
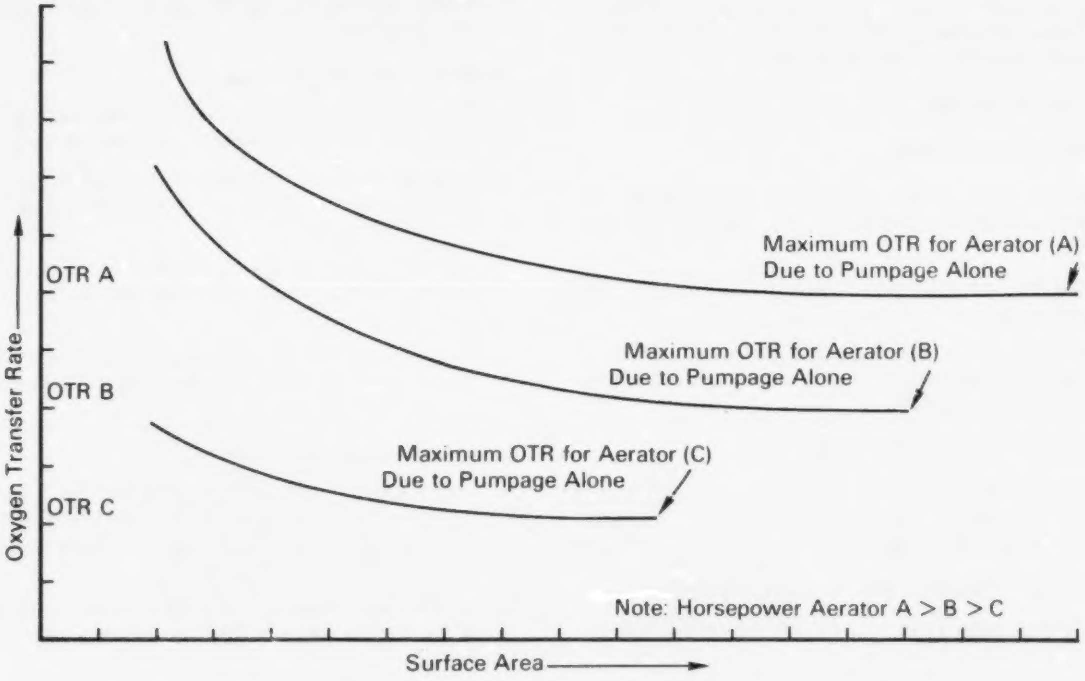


Figure 2. Oxygen Transfer Rate vs Surface Area at Fixed Depth for Mechanical Surface Aerators



effect on oxygen transfer rate. The actual limit differed depending on the type of aerator.

(c) The limit of each aerator was determined by its actual pumping capacity. At this limit the oxygen transfer efficiency was the same for all horsepower sizes if the design was consistent. Any increase in efficiency beyond this defined limit is due to the unique mixing combinations and circulation patterns established by a specific geometry.

Aerator Testing Significance

1. The measured oxygen transfer efficiency of a specific aerator can be increased as much as 50% by the influence of tank geometry.
2. This geometry effect can be measured and quantitatively defined for each aerator.
3. Each aerator has an oxygen transfer rate which is independent of geometry which can also be determined.

Aeration Process Significance

1. Multiple aerator installations in large basins cannot achieve the oxygen transfer rate of single units in test volumes due to the inability of reproducing the same mixing and circulation conditions.
2. Figure 3 compares the single unit influenced by test basin geometry with multiple units of the same size in a large basin. The linear portion of the lower curve is a function of the multiple unit pumping capacity while at the upper end there is an increase in efficiency due to more energy input sites. It is unlikely that this increase will ever exceed more than 10% of the efficiency due to pumpage alone.

Submerged Turbines

Types of Aerators Tested

All turbines were bridge mounted overhung shafts without a bottom support and operated at speeds from 30 to 100 rpm.

1. Radial Turbines (3 basic configurations) — Mounted above a static air sparger.
2. Down Flow Axial Turbines (2 basic configurations) Mounted either above a static air sparger or with a rotating shear device.

Specific Basin Geometries Tested

Basin Dimensions	Volume (gal)
a. 20′ square x 18′ deep	54,000
b. 40′ square x 16′ deep	190,000

Influence of Tank Geometry on Oxygen Transfer

1. Oxygen transfer rate per unit volume vs air flow — Figure 4:

   (a) Mixing conditions are established by the mechanical turbine without air. The intercept of the aerator response curve indicates an initial oxygen transfer rate as soon as air is supplied. As long as this condition remains constant, there is a linear response between oxygen transfer rate per unit volume and air supply up to the point where the turbine is "flooded" (Curve B).

   (b) Reducing the input turbine horsepower in the same geometry will generate a parallel response curve at a lower level (Curves B and C).

   (c) The change in slope between the 20 ft x 20 ft basin (Curve A) and the 40 ft x 40 ft basin (Curve B and C) indicates a somewhat different hydraulic mixing regime at the same depth independent of P_m/V.

2. Oxygen transfer efficiency as a percent of the input oxygen in the air supply Figure 5:

   (a) The data in Figure 5 are a portion of the total response curve and are presented as a generalized slope only to demonstrate the relative effect of depth and input turbine horsepower.

   (b) All geometries tested decreased in percent efficiency at higher air to volume ratios; however, increasing the turbine power at the same depth also increased the range of efficiencies (Curves B and C).

   (c) Raising the water level improved the percent transferred even though the ratio of input turbine horsepower to volume decreased.

   (d) The net effect of maximum depth and high mixing turbine power to volume ratios will produce optimum efficiencies particularly at oxygen transfer rates over 150 mg/l·hr.

Aerator Testing Significance

1. The influence of geometry on submerged turbines is more complex than surface mechanical aerators.
2. It appears that the mixing conditions established in a specific geometry by the mechanical turbine will be the overriding factor.
3. Increasing the depth of a specific basin increases the oxygen transfer rate at a lower turbine horsepower to volume ratio.
4. The performance of submerged turbine aerators can be changed 100% by selecting the geometry and input power/volume ratio.

Aeration Process Significance

1. Fortunately, submerged turbines are often operated as single units in one basin where full-scale testing can be employed. This would allow high rate systems to be designed and verified.
2. Multiple installations are likely to have operating characteristics which differ greatly from single unit tests.

Figure 3. Oxygen Transfer Rate Per Unit Volume vs Total Shaft Horsepower of Mechanical Surface Aerators

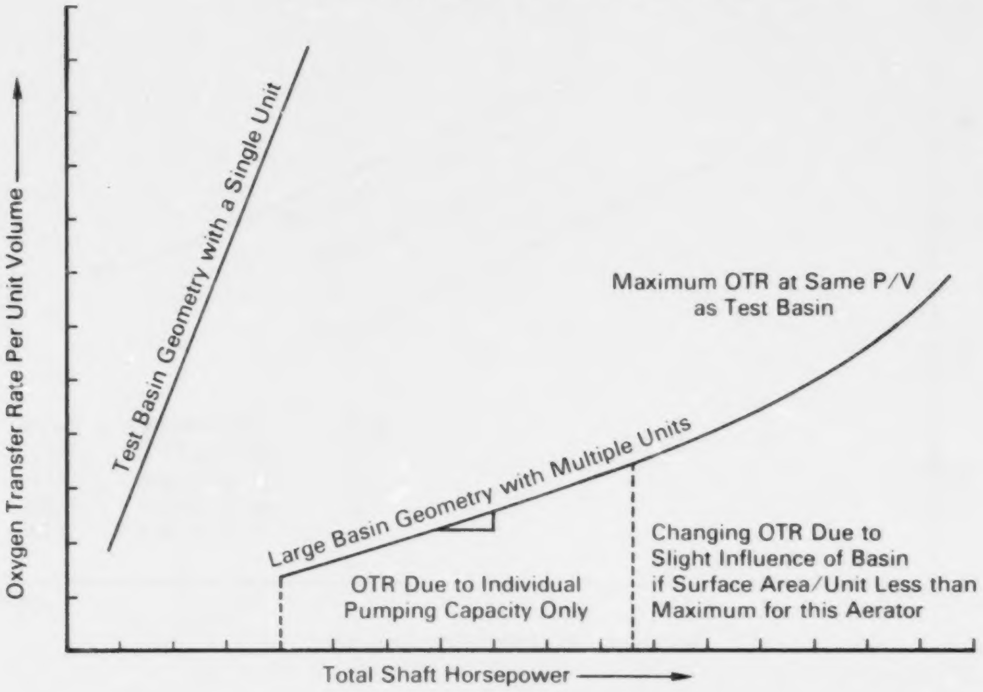
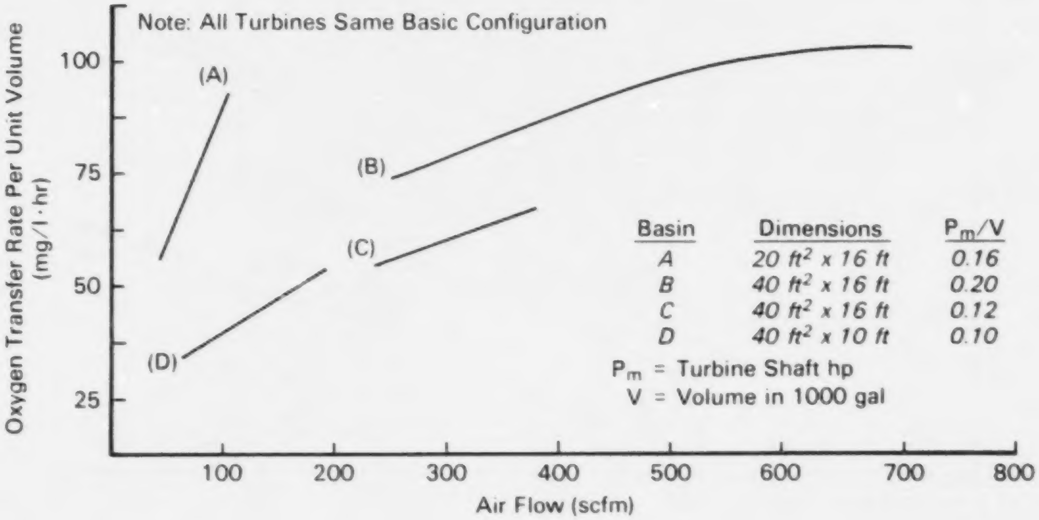


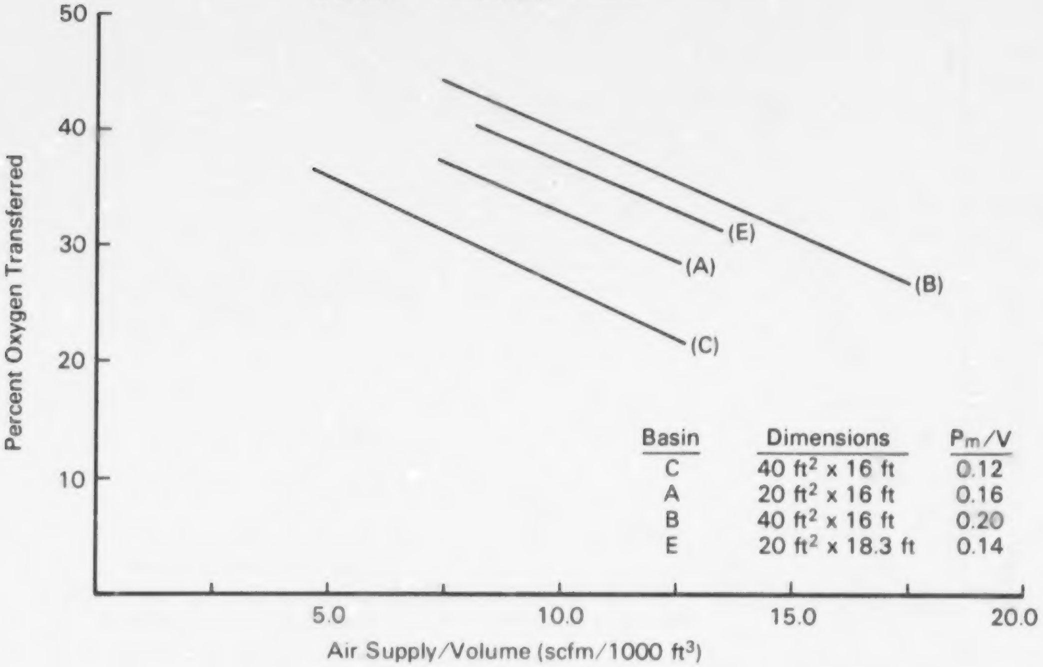
Figure 4. Oxygen Transfer Rate Per Unit Volume vs Air Flow for a Submerged Turbine



Basin	Dimensions	P_m/V
A	*20 ft² x 16 ft*	*0.16*
B	*40 ft² x 16 ft*	*0.20*
C	*40 ft² x 16 ft*	*0.12*
D	*40 ft² x 10 ft*	*0.10*

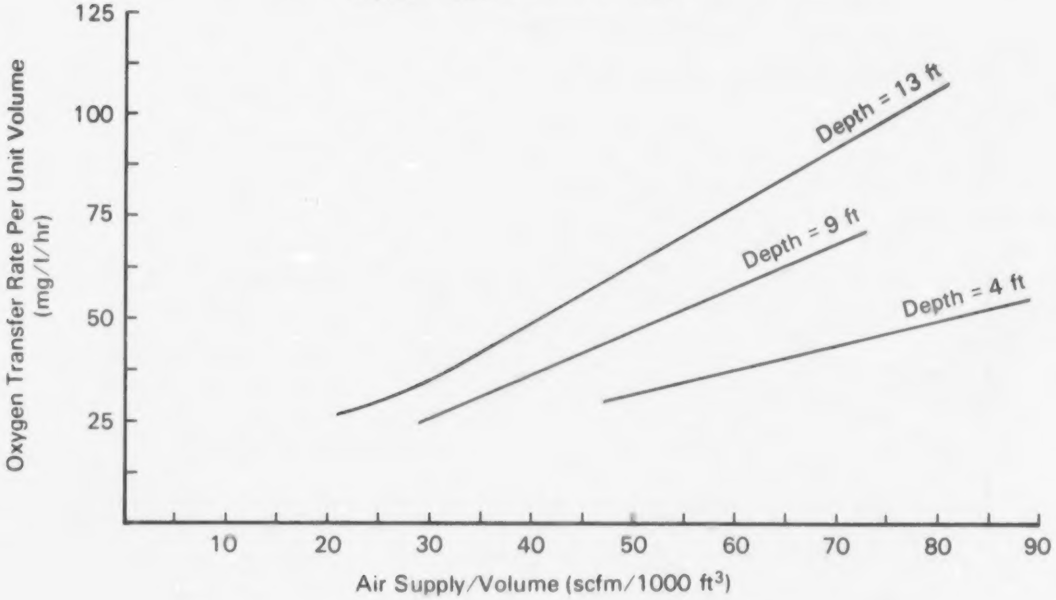
P_m = Turbine Shaft hp
V = Volume in 1000 gal

Figure 5. Percent Oxygen Transferred to Water from Air Supply vs Air Supply/Volume



Basin	Dimensions	P_m/V
C	40 ft² x 16 ft	0.12
A	20 ft² x 16 ft	0.16
B	40 ft² x 16 ft	0.20
E	20 ft² x 18.3 ft	0.14

Figure 6. Oxygen Transfer Rate Per Unit Volume vs Air Supply/Volume in 16 ft² Tank



Diffused Aeration

Types of Diffusers Tested

1. The influence of geometry on fine bubble or porous media diffusers is part of a current study and not available at this time.
2. The coarse bubble diffuser data presented were developed with two types of diffusers manufactured by Envirex Inc. Side-by-side comparative tests of these two diffusers and other designs have demonstrated that the type of coarse bubble diffuser itself has little influence on oxygen transfer.

Specific Tank Geometries Tested

Basin Dimensions	Volume (gal)
a. 16′ square x 15′ deep	29,000
b. 20′ square x 20′ deep	60,000
c. 30′ wide x 6′ long x 21′ deep	29,000
d. 30′ wide x 80′ long x 21′ deep	380,000

Influence of Tank Geometry on Oxygen Transfer

1. Oxygen transfer rate per unit volume vs airflow per unit volume.

   (a) Figure 6 (16 ft square x 15 ft deep). There is a linear response between the oxygen transfer rate and increasing air flow per volume at each depth from 4 ft to 13 ft.

   (b) Figure 7 (30 ft x 6 ft x 21 ft deep). The 13 ft depth response line corresponds reasonably well with the 16 ft x 16 ft geometry in Figure 6; however, at 19 ft, the slope is not predicted. For comparison, installing sock diffusers at the same location develops an entirely different type of response curve.

   (c) Figure 8 (20 ft square x 20 ft deep). At 15 ft, the effect of depth follows the 13 ft and under response of the previous two geometries, but the 20 ft curve demonstrates a very rapid increase in transfer rate with only a slight change in air flow.

   (d) Figure 9 (30 ft x 80 ft x 21 ft deep — diffuser pattern "A"). The linear effect of increased air flow at each depth 13 ft and below developed in previous geometries is continued up to 19 ft. The magnitude of the oxygen transfer rate at 19 ft is less that that found at 15 ft in the 20 ft square tank (Figure 8).

   (e) Figure 10 (30 ft x 80 ft x 21 ft deep — diffuser pattern "B"). The effect of diffuser pattern is demonstrated when compared to Figure 9. For this pattern, the 16 ft depth is slightly above the 15 ft curve in the 20 ft square tank (Figure 8) indicating good correlation for this one depth only.

Aeration Test Significance

1. For a specific geometry there will be a linear increase in the oxygen transfer rate per unit volume as the air flow increases *only* if the established mixing conditions remain the same. A non-linear response indicates a change in these conditions which may or may not be accomplished in a dissimilar geometry.
2. At depths less than 15 ft there is little effect of geometry although diffuser placement can affect results by 25%.
3. At depths over 15 ft when one or more of the tank surface area dimensions are less than the water depth, a potential exists for creating "unique" geometry effects.
4. The influence of geometry and diffuser patterns can affect results at depths over 15 ft up to 100% which renders any definition of oxygen capacity for a unit diffuser meaningless.

Aeration Process Significance

1. Large-scale diffused aeration systems designed on the basis of data from aeration tests which were biased by tank geometry may not meet process requirements.
2. The linear response of the oxygen transfer rate per unit air supplied without the influence of tank geometry could lead to more predictable diffused aeration systems.

General Conclusions

1. Basin geometry does affect the mixing regime established by a specific aeration device and thereby significantly influences its oxygen transfer rate.
2. Oxygen transfer rates based on testing in "unique" geometry configurations may not be representative of full-scale designs.
3. For all major aeration devices the influence of basin geometry is a definable parameter which could lead to more efficient aeration basin designs.

Recommendation to the Committee

A program to develop data on the influence of basin geometry for all types of aeration equipment would appear to be a venture beyond the scope of this Subcommittee. It should remain the responsibility of the equipment manufacturer to identify the operating characteristics of their own equipment.

At the present time, the design engineer is faced with the difficult task of selecting aeration equipment whose performance may be biased by either test basin geometry, or test procedure, or both. If this Subcommittee is successful in establishing a standardized test procedure, the engineer can start to compare oxygen transfer efficiency claims and determine whether or not a manufacturer has thoroughly defined all the operating characteristics.

It would seem more likely to fall on the EPA to confirm that basin geometry does influence oxygen transfer for a specific device. Without that confirmation, any cost effective comparison could err greatly by comparing the worst situation for one device with the biased oxygen transfer efficiency of another.

Figure 7. Oxygen Transfer Rate Per Unit Volume vs Air Supply/Volume in 30 ft x 6 ft Tank

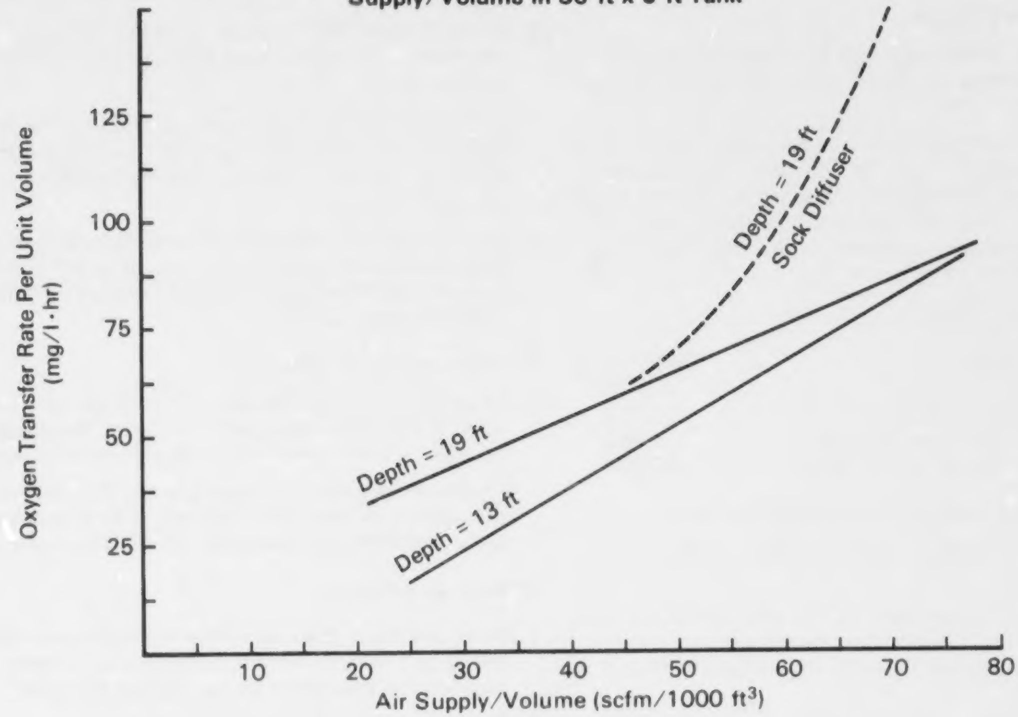


Figure 8. Oxygen Transfer Rate Per Unit Volume vs Air Supply/Volume in 20 ft² Tank

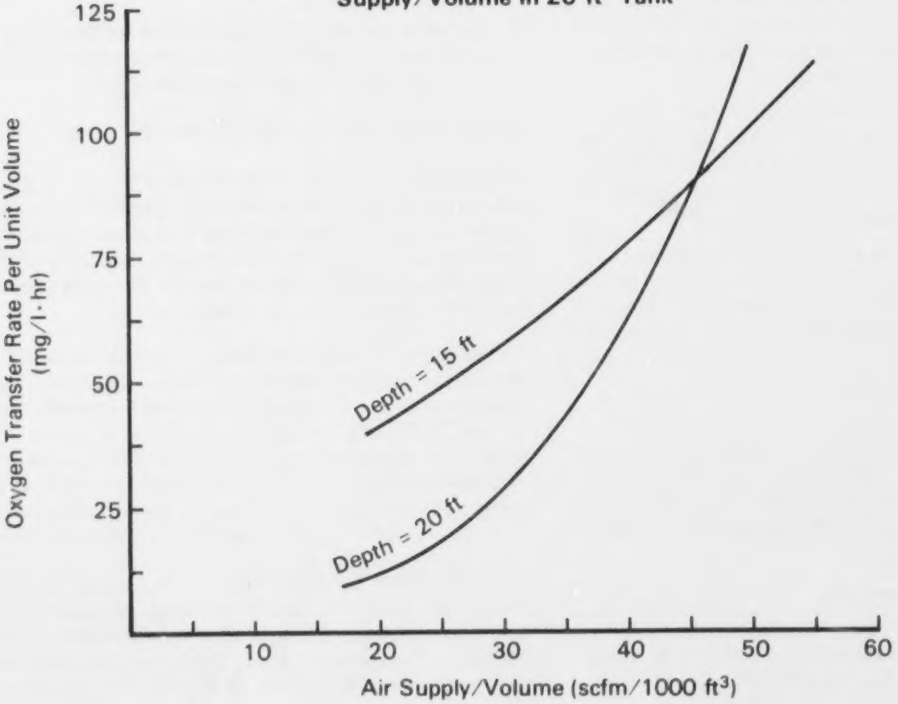


Figure 9. Oxygen Transfer Rate Per Unit Volume vs Air Supply/Volume in 30 ft x 80 ft Tank with Diffuser Pattern "A"

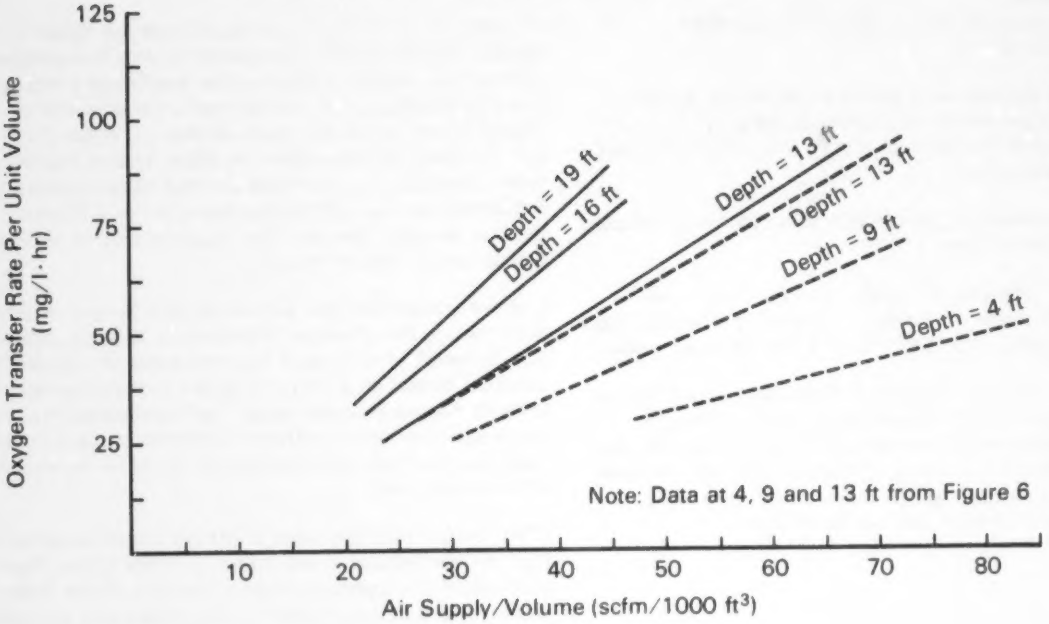
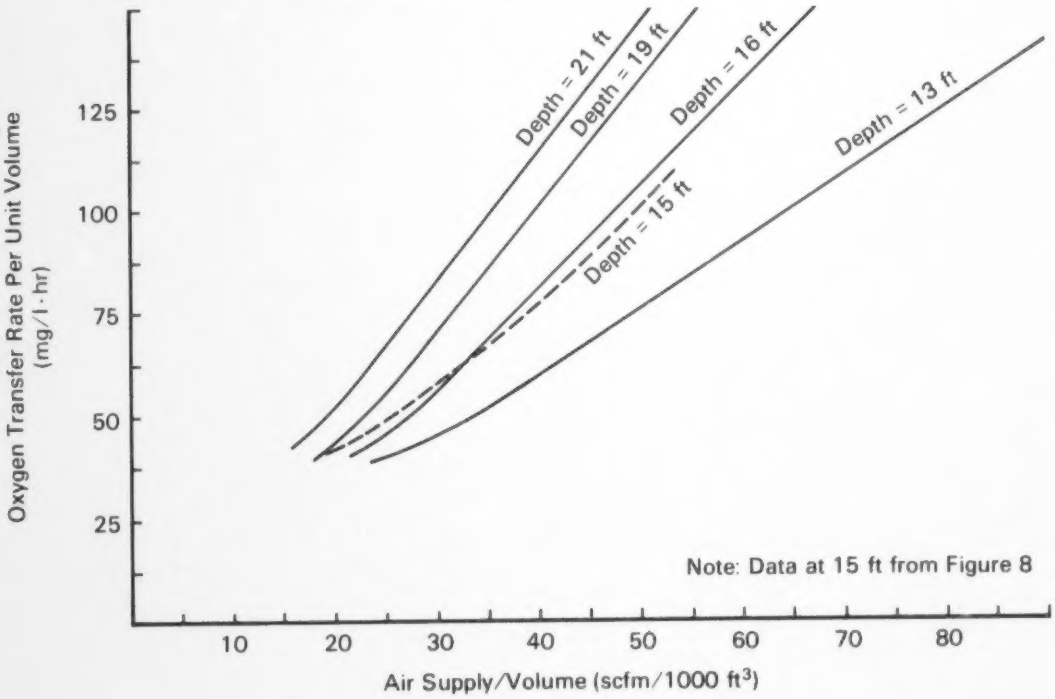


Figure 10. Oxygen Transfer Rate Per Unit Volume vs Air Supply/Volume in 30 ft x 80 ft Tank with Pattern Diffuser Pattern "B"



Discussion

Jerome D. Wren
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Milwaukee WI 53201

The report by Rooney was based on work conducted by one manufacturer on various types of aeration devices. It is suggested that the work of others should be included, at least by reference.

Several references on the subject, as it relates to diffused aeration are as follows:

Bewtra, J.K. and W.B. Nicholas, "Oxygenation from Diffused Air in Aeration Tanks", Journal Water Pollution Control Federation, Vol. 36 (10), p. 1195, October 1964.

Schmit, F.L., J.D. Wren, and D.T. Redmon, "The Effect of Tank Dimensions and Diffuser Placement on Oxygen Transfer", Presented at the New England Water Pollution Control Association Meeting, Dixville Notch, NH on June 9, 1976, (Scheduled for publication in the July 1978 Journal Water Pollution Control Federation).

Closure

Thomas C. Rooney

Mr. Wren's comment is correct in that my report to the Subcommittee is work conducted by one manufacturer. It was not our intent to slight other studies or investigations by omission, but neither were we presenting a literature review on the state-of-the-art. While such a survey would be a worthwhile effort, in this instance we were attempting to demonstrate that basin geometry had influenced the rate of oxygen transfer for different generic aeration devices. The purpose was to generate discussion at the Workshop.

It is our contention that ultimately the burden of proof in determining the absence or presence of basin geometry effects remains with each manufacturer of a specific aeration device. We also concluded that comparative tests of oxygen transfer equipment conducted in research-scale aeration tanks, without confirmation on a scale at least approaching full-size design, could be in error by some magnitude.

If Mr. Wren feels the work of others would shed further light on the subject, it would be contrary to the stated purpose of the Subcommittee to exclude these references. Our only reservation is that these two suggested articles also do not represent the sum total of documented studies in this field.

Influence of Mixing in Aeration

11

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Introduction

Mixing is a key element of aerator performance. Whether the aeration is accomplished by a mechanical surface device, subsurface air, or mechanical dispersion of subsurface air, the basic mixing requirements must be satisfied. Mixing is defined as the circulation which convects the oxygen enriched fluid throughout the basin and provides the degree of agitation necessary to maintain solids suspension.

A precise process model would separate the mass transfer at the gas liquid interface from the ensuing convective dispersion in the tank. However, since a simplified first order differential is generally used to describe the overall process, mixing effects are intrinsic to the mass transfer model. Thus, the mass transfer coefficient is indicative of the mixing and mass transfer interactions.

Surface Aeration

The interaction of aerator performance and mixing is illustrated in the first series of figures. Figure 1 is a schematic of a typical surface aerator operation. In analyzing the surface aerator clean water performance, a first order mass transfer model is used to characterize the oxygen uptake rate. The rate of oxygen absorption is expressed in the form:

$$dC/dt = K_La\,(C^*-C) \qquad (1)$$

where the mass transfer coefficient, K_La, is determined by analyzing the change in bulk dissolved oxygen level in the basin as a function of time. In this form, the equation implies that oxygen transfer is occurring throughout the basin during the period of observation. As illustrated in Figure 1, the aeration zone comprises only a small percentage of the total volume. The oxygen enriched liquid is then distributed throughout the basin by means of the pumping action of the aeration device. Therefore, in employing the above equation, the mass transfer coefficient determined characterizes a combination of the basin hydraulics and the mass transfer occurring at the liquid-gas interface.

Other assumptions implicit in this simplified surface model are:

1. Oxygen transfer occurs at the prevailing surface conditions of temperature and barometric pressure, and the oxygen concentration in the gas remains constant.
2. Equilibrium prevails at the gas-liquid interface and is governed by Henry's Law.
3. The liquid film comprises the major resistance to mass transfer.
4. Environmental factors other than barometric pressure are negligible.
5. A well-mixed condition exists in the basin such that a meaningful average DO level can be determined from the sample point measurements.

Figure 2 illustrates a less than ideal utilization of a surface aerator. For reasons of tank geometry or improper impeller sizing criteria, the primary bulk flow includes only the upper portion of tank volume; the weak secondary flow pattern has been induced in the lower section. A reaeration test would show the lower region achieving a slower oxygen uptake rate and possibly lower bottom velocities as well. In a waste operation, this system could result in sludge buildup and anaerobic digestion.

Assuming that neither the aerator nor basin can be altered, a lower mixing impeller is necessary to alleviate this problem. Several alternatives are available. Figure 3 illustrates one approach to solving this problem. An uppumping lower impeller is installed near the bottom and depending on the particular operating condition, this can break the short circuiting flow pattern, establishing a single top to bottom flow regime.

In situations where the lower flow pattern is highly developed, it is more efficient to reinforce the lower pattern rather than oppose it. Hence, Figure 4 shows a downpumping lower impeller as a possible solution to the similar problem illustrated in Figure 2. This procedure may be preferable in those waste streams where solids suspension is considered a major problem. As flow is directed strongly to the bottom of the basin, high bottom velocities can be maintained.

Oxygen is transferred to the lower pattern by diffusion through the contact zone of interchange. Sufficient power must be supplied to the mixing impeller to cause vigorous and turbulent motion in this region. For waste operation, a dissolved oxygen gradient may exist, but positive DO levels will be maintained at all points.

One further method by which this situation can be remedied is through the use of a draft tube. This must be of sufficient length and design so as to permit solids to

Figure 1. Schematic Visualization of Ideal Surface Aerator Operation

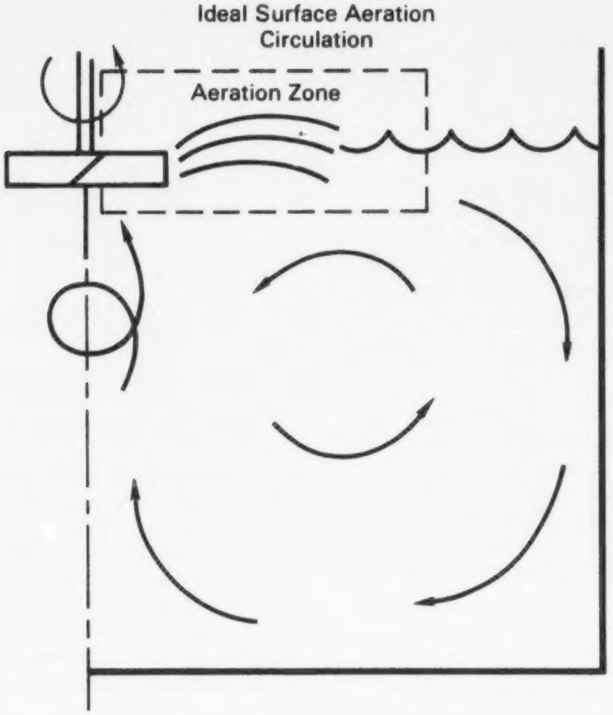

Ideal Surface Aeration
Circulation
Aeration Zone

Figure 2. Surface Aeration with Induced Secondary Flow

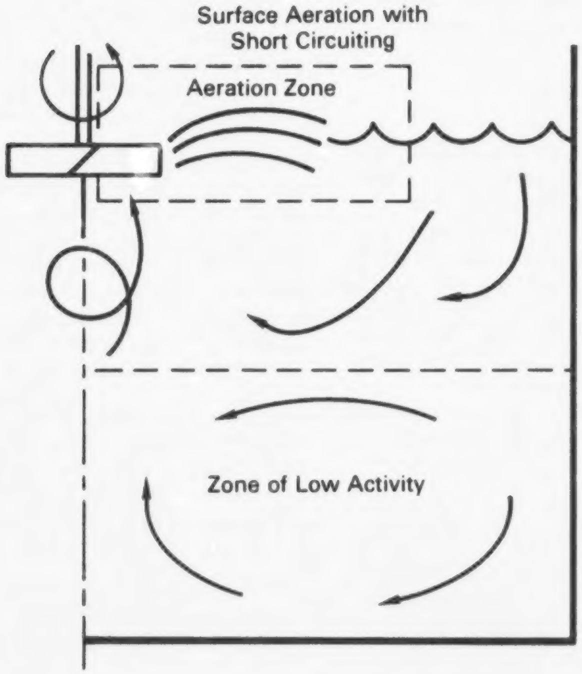

Surface Aeration with
Short Circuiting
Aeration Zone
Zone of Low Activity

Figure 3. Surface Aerator with Uppumping Lower Impeller — Single Flow Pattern

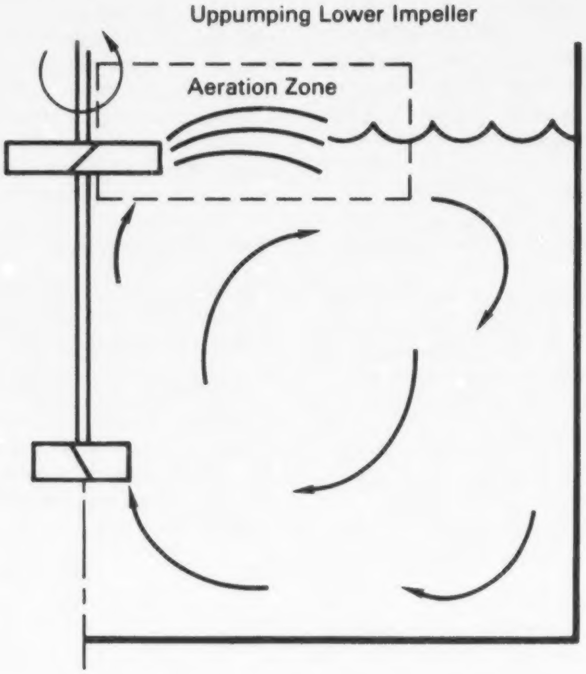
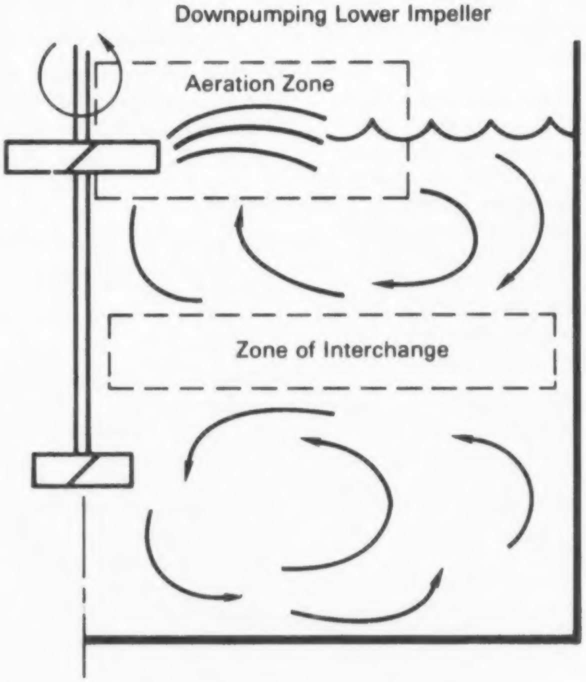

Uppumping Lower Impeller
Aeration Zone

Figure 4. Surface Aerator with Downpumping Lower Impeller — Dual Flow Pattern


Downpumping Lower Impeller
Aeration Zone
Zone of Interchange

be drawn off the basin bottom.

To clarify these points further, surface reaeration tests in a 223 m^2 by 9.09 m deep basin (2028 m^3) have been made comparing performance with lower uppumping and downpumping impellers. For both tests, the surface and submerged impellers used were exactly the same. A provision was made so that the angle of attachment of the blades on the lower impeller could be changed so as to convert from uppumping to downpumping modes. The power draw was approximately 70 shaft hp for the surface aerator and 25 shaft hp for the lower impeller.

The clean water reaeration test data corresponding to a well-mixed single flow pattern (lower uppumping impeller, Figure 3) are presented in Figure 5. The circles represent data at a surface sample point while the triangles are taken from a lower sample point. The net result is that at each pump location, the measured rate of oxygen transfer (the slope of the line) is the same. Displacement of the lines with respect to one another delineates the time delay in convection of the oxygen enriched fluid to respective portions of the basin. The magnitude of this shift is dependent on the relative rates of oxygen transfer and the blending.

For a surface aerator with a dual flow pattern (Figure 4), the reaeration data will look somewhat different from the case just cited. This difference is depicted in Figure 6. The slopes of the DO deficit vs time curves are different for respective sample points near the surface and bottom. There is also a displacement of the lines, which is an indication of delay in convecting the oxygen enriched fluid. This displacement is greater than for the previously cited case because of the additional resistance created by the zone of interchange.

A comparison of Figures 5 and 6 demonstrates how blending is an intrinsic and integral part of the mass transfer process. Since both configurations employ the same surface aerator at the identical speed, both devices should be capable of delivering the same number of kg/hr (lb/hr) of oxygen. Yet the unit with the dual flow pattern yielded an oxygen transfer rate 75% of that for the single flow pattern aerator. This provides convincing evidence that the defined mass transfer coefficient is an overall coefficient reflective of the combined mass transfer and convection processes within the basin.

A second element of aerator performance is the ability to suspend solids. The data presented in Figures 5 and 6 clearly suggest that the downpumping impeller provides a far stronger flow field along the basin bottom. Therefore, this unit might be preferable in waste streams with a documented solids suspension problem.

The data presented in Figures 5 and 6 represent an unusual application for a surface aerator as a 70-shaft-hp surface aerator is not generally applied for mixing and aerating a basin 9.09 m in depth.

These examples demonstrate the complex interaction in performance criteria. While the uppumping lower mixing impeller provides a single flow pattern and higher transfer efficiencies, the associated bottom velocities of this unit are on the order of one-half a foot per second. For the downpumping lower mixing impeller, the transfer efficiency diminishes (this unit delivered 75% of the oxygen transferred with uppumping for the same horsepower) but the bottom velocities are higher and suitable for wastes with persistent solids suspension problems.

Subsurface Aeration — Theory

The simplest subsurface aeration device is a single orifice bubbler. With this device, a string of constant diameter bubbles are released and rise to the surface under the effects of gravity. Figure 7 (5) shows the empirical relationship determined to express bubble size of air in water as a function of gas velocity from a 0.061 cm orifice (1). The bubble diameter for gas rates less than the critical value is a constant. The functional relationship for specifying this bubble size has been determined to be of the form:

$$d_b^3 = K_1[d_o\sigma/(\rho_l-\rho_g)] \qquad (2)$$

where:

d_b = bubble diameter (L)
K_1 = a constant
d_o = orifice diameter (L)
σ = surface tension (m/L)
ρ_l = liquid density (m/L^3)
ρ_g = gas density (m/L^3)

At high gas flow rates, the formation of bubbles is hinhindered by the presence of the preceding bubble. This transition point is defined by a critical velocity:

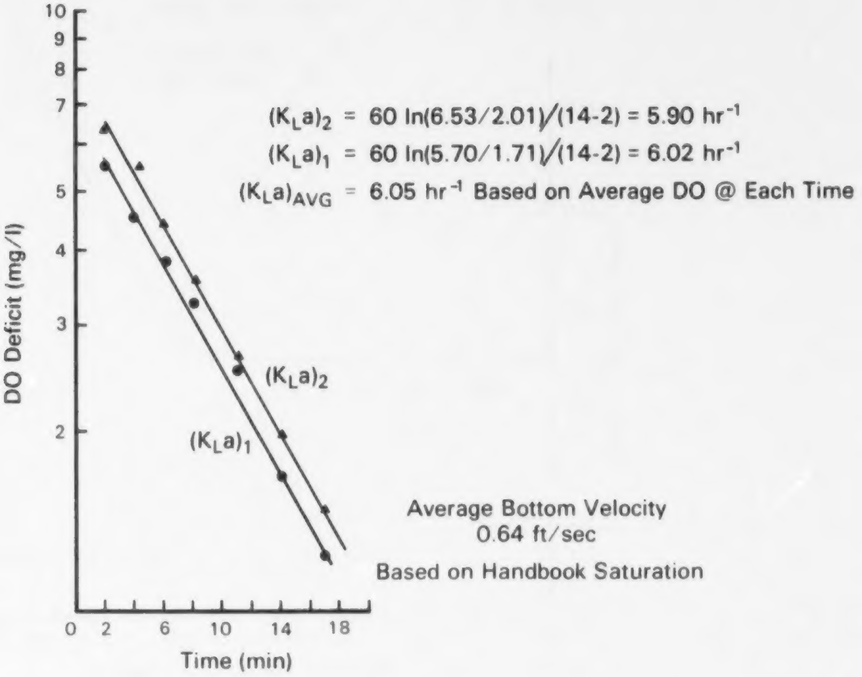
$$v_{CRIT}^3 = K_2\sigma^4/[\mu_l^3 d_o^2(\rho_l-\rho_g)] \qquad (3)$$

where:

v_{CRIT} = critical bubble velocity (m/t)
K = a constant
μ_l = liquid viscosity (f t/L^2)

Other studies (2) (4) have been devoted to determining bubble rise terminal velocity as a function of diameter (Figure 8). Combining the information of Figures 7 and 8, exact relationships are obtained for interfacial surface area and bubble residence time. For low stripping efficiencies and shallow depths (30-180 cm), Coppoch and Meiklejohn (1) were able to determine K_L independent of a. This is a pure mass transfer coefficient untainted by convection or turbulence. These results are presented in Figure 9. Due to the physics of the experiment, the key elements have been quantified within a narrow range.

Figure 5. Reaeration Test Data — Single Flow Pattern



$(K_La)_2 = 60 \ln(6.53/2.01)/(14\text{-}2) = 5.90\ \text{hr}^{-1}$

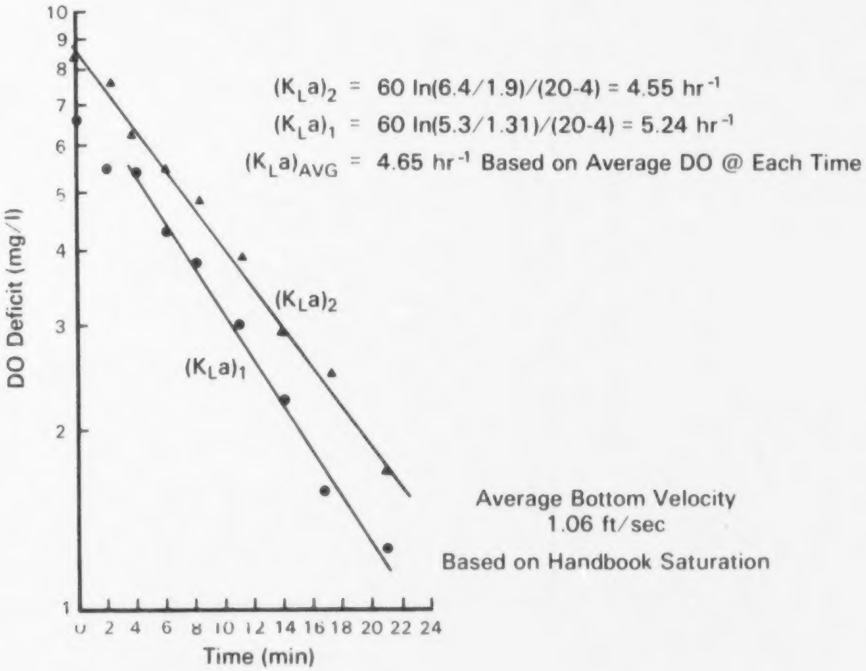
$(K_La)_1 = 60 \ln(5.70/1.71)/(14\text{-}2) = 6.02\ \text{hr}^{-1}$

$(K_La)_{AVG} = 6.05\ \text{hr}^{-1}$ Based on Average DO @ Each Time

Average Bottom Velocity
0.64 ft/sec

Based on Handbook Saturation

Figure 6. Reaeration Test Data — Dual Flow Pattern



$(K_La)_2 = 60 \ln(6.4/1.9)/(20\text{-}4) = 4.55\ \text{hr}^{-1}$

$(K_La)_1 = 60 \ln(5.3/1.31)/(20\text{-}4) = 5.24\ \text{hr}^{-1}$

$(K_La)_{AVG} = 4.65\ \text{hr}^{-1}$ Based on Average DO @ Each Time

Average Bottom Velocity
1.06 ft/sec

Based on Handbook Saturation

Figure 7. Variation in Bubble Diameter with Gas Velocity

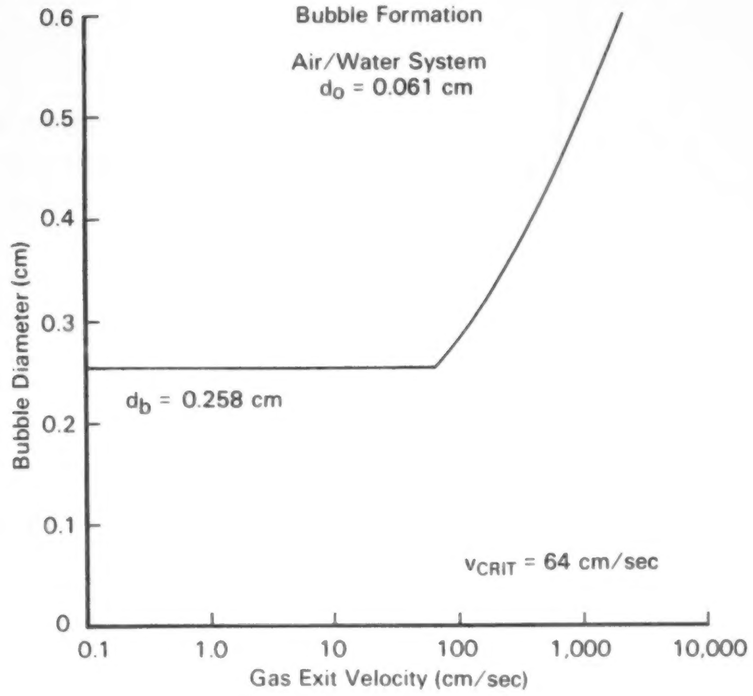
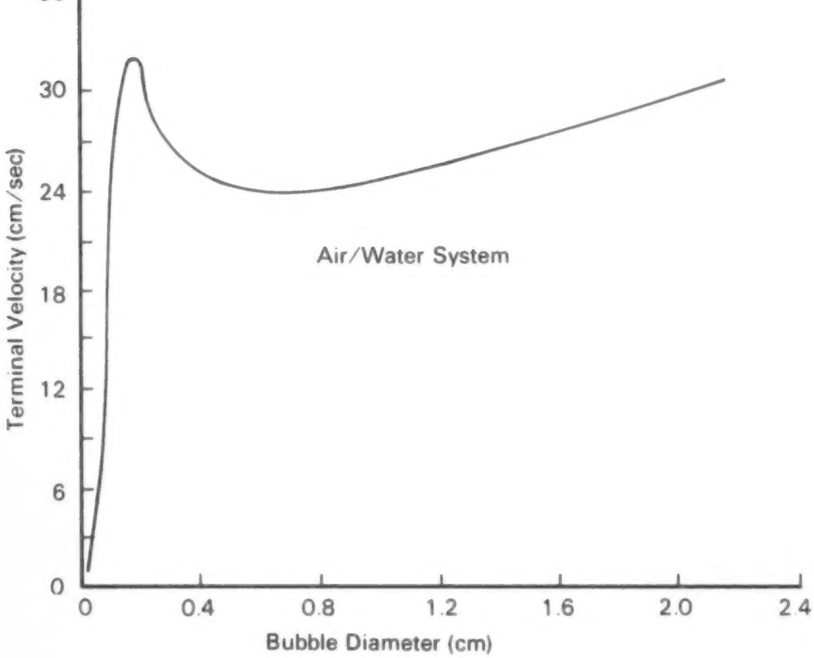


Figure 8. Variation of Terminal Velocity with Bubble Diameter



These key elements are the bubble size, the relative velocity and the residence time. Even with such well defined conditions, there is still considerable data scatter (or uncertainty) for defining a functional relationship between bubble size and mass transfer. In aeration processes, submerged impellers, pumps and jet devices create conditions of extreme shear gradients and high levels of turbulence. There are no technical treatises dealing with bubble size as a function of these flow characteristics.

Subsurface Aeration — Current Practice

In any commercial subsurface aeration system, the controlling parameters are: 1) bubble size, 2) relative velocity and 3) residence time. These variables are in turn dependent upon the physical environment surrounding the bubble release and flow path. The combined effects of turbulence, buoyancy and interfacial tension control the fragmentation of the gas phase as it is released while the size distribution of the bubbles and the hydraulic flow patterns, within the basin, control the residence time of the gas. Dispersion, coalescence and convection characteristics are all dependent on the particular device and basin geometry.

For submerged aerators, a simplified first order differential equation is used to define the reaeration process. Practically all mass transfer models for submerged aeration take the form:

$$dC/dt = K_L a\, D_f \tag{4}$$

where D_f can represent any one of several driving force relations. This driving force is defined as the difference between the liquid film oxygen concentration in equilibrium with the gas bubble and the surrounding bulk liquid dissolved oxygen.

Basically, there are two categorical definitions for the driving force. The first group of models utilize a variable value for saturation during the reaeration process. That is, the effective equilibrium saturation value is dependent on a changing oxygen content in the gas stream and dissolved oxygen in the liquid. As a result, during the clean water reaeration process, the value of this saturation changes with these variables. Examples include "log-mean driving force", "log-mean saturation" and others as described in Stanton and Bradley (6) and Lakin et al. (3).

The second group of models are based on a representative constant saturation value for the entire reaeration process. This category includes variations of pseudo-surface aeration models.

For both cases, the driving force is dependent upon bulk properties of the gas and liquid phases. By definition, the K_La term must necessarily envelop all of the aerator characteristics associated with elevating the oxygen level in the basin. Hence, the characteristic bubble size, relative velocity, retention time and convective flow patterns are all lumped into this single mass transfer parameter. Any other approach would require an extremely complex process equation and sophisticated test procedure which are well beyond the technology currently available.

Mixing Measurement

The most direct indication of adequate mixing is provided by the dissolved oxygen data. Unduly large gradients in either the waste basin or in the reaeration clean water test would indicate an improper balance between mixing and aeration. To illustrate the relationship between gradients, mixing and aeration, consider a two zone model based on Figure 1. This model is one order of magnitude more complex than the approach used to obtain the differential equation for the liquid phase, Equation 1. Oxygen transfer for this model occurs only in the "aeration" zone. The reaeration of the basin occurs as a result of convective mixing from the aeration zone to the major portion of the basin.

To model this process, a differential equation for each zone is required. These equations are interdependent as a result of the bulk flow passing between the zones. For the aeration zone we have:

$$\rho_l V_A(dC_A/dt) = \rho_l Q_B(C_B-C_A)+\rho_l V_A K_L a(C^*-C_B) \tag{5}$$

Similarily for the circulation zone:

$$\rho_l V_B(dC_B/dt) = \rho_l Q_B(C_A-C_B) \tag{6}$$

where:

V_A = liquid volume of aeration zone (L^3)
V_B = liquid volume of circulation zone (L^3)
C_A = DO concentration in liquid phase, aeration zone (m/L^3)
C_B = DO concentration in liquid phase, circulation zone (m/L^3)
C^* = DO saturation in liquid phase (m/L^3)
Q_A = pumping rate in aeration zone (L^3/t)
Q_B = pumping rate in circulation zone (L^3/t)

Note that the inlet flow to the aerator originates in the circulation zone. Thus, the mass transfer driving force is the difference between saturation and the dissolved oxygen level from that region. For the case of very large values of the pumping rate Q_B, the concentration C_A approaches C_B. Equations 5 and 6 then reduce to Equation 1.

This formulation of the aeration process is the first step toward a rigorous solution to reaeration performance. The actual system will require much more compartmentalization to accurately model the process. For each basin region, the bulk dissolved oxygen level and the fluid interchange between adjacent regions must be determined.

Recognizing that this cellular model is a simplified attempt to interpret the balance between mixing and aeration, the equations are solved for a specific example. A hypothetical aeration basin which contains 3378 m^3 of water is divided as follows: 20% for the aeration zone and 80% for the convective zone. Based on this volume split, the bulk dissolved oxygen concentration is defined as:

$$C = 0.2\ C_A + 0.8\ C_B \tag{7}$$

Equation 5 and 6 are solved for pumping rates, Q_B, varying from 189 to 1135 m^3/min. The reaeration variables one could expect to measure are determined as a function of the pumping rate.

It is apparent that four different mass transfer coefficients can be defined using this approach. The first, from Equation 5, begins to resemble a "true mass transfer coefficient" in that K_La is divorced from the convective process and addresses only the oxygen transfer in the aeration region. The other three values of K_La are obtained from utilizing Equation 1 and the concentration-time histories for each region individually as well as for the defined bulk average.

To provide a comparison of these four mass transfer coefficients, it is convenient to assume, for illustrative purposes, that the rate of oxygen transfer provided in the aeration zone (as defined by Equation 1) is identical for each pumping rate chosen. Table 1 presents the results of these computations.

Corresponding values of the oxygen transfer rates are provided in Table 2. It is observed that at low pumping rates, the oxygen transfer capabilities are substantially overstated by using Equation 1 in the aeration zone. However, as the pumping capacity is increased to 757 m^3/min or greater, these differences are reduced to within the range of experimental uncertainty. The derivation of Equation 1 requires that the bulk dissolved oxygen be used in application of the model. Therefore, the best indicator of performance is given by column 4 of Table 2. To experimentally obtain an adequate representation of the basin bulk dissolved oxygen level, it is necessary to use a sufficient number of sample points.

In pursuing this example further, extension of the analysis is helpful in examining the relationship between mixing, mass transfer and dissolved oxygen gradients during a reaeration test. A graphical presentation of the dimensionless dissolved oxygen gradient vs time per basin turnover is provided in Figure 10. The pumping capacities selected embrace a significant range of turnover times for activated sludge aeration basins. Therefore, the dimensionless gradient is shown to be an inverse function of the aerator pumping capacity. The gradient is also dependent on the mass conductance of the system as indicated in Figure 10.

The significance of the term $(C_A-C_B)/(C^*-C)$ can best be demonstrated by an example. Consider the case when this

Table 1. Mass Transfer Coefficient Comparison

		K_La, Equation 1 (hr^{-1})		
Pumping Rate (m^3/min)	K_La, Eqns. 5&6 (hr^{-1}) Two Zone Model	Aeration Zone	Convective Zone	Basin Bulk Average
189	2.03	2.85	2.16	2.26
378	2.49	2.85	2.62	2.65
757	2.70	2.85	2.77	2.79
1135	1.74	2.85	2.79	2.79

Table 2. Performance

	Oxygen Transfer at Test Conditions (kg/hr)			
Pumping Rate (m^3/min)	Aeration Zone	Convective Zone	Basin Bulk Average	Percent Deviation of Aeration Zone From Bulk Average
189	78.9	59.9	62.6	26
378	78.9	73.0	73.9	6.8
757	78.9	76.7	77.6	1.8
1135	78.9	77.6	78.0	1.2

Figure 9. Bubble Diameter vs Mass Transfer Coefficient — Oxygen/Water System

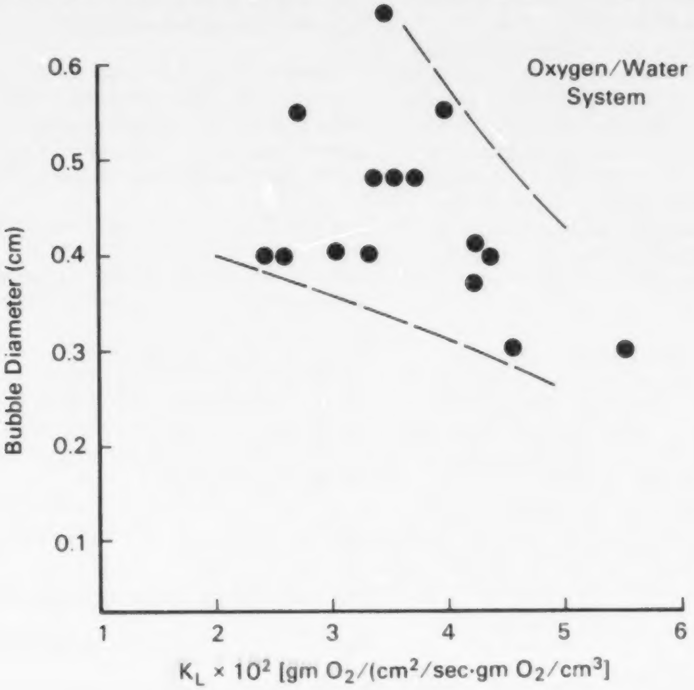
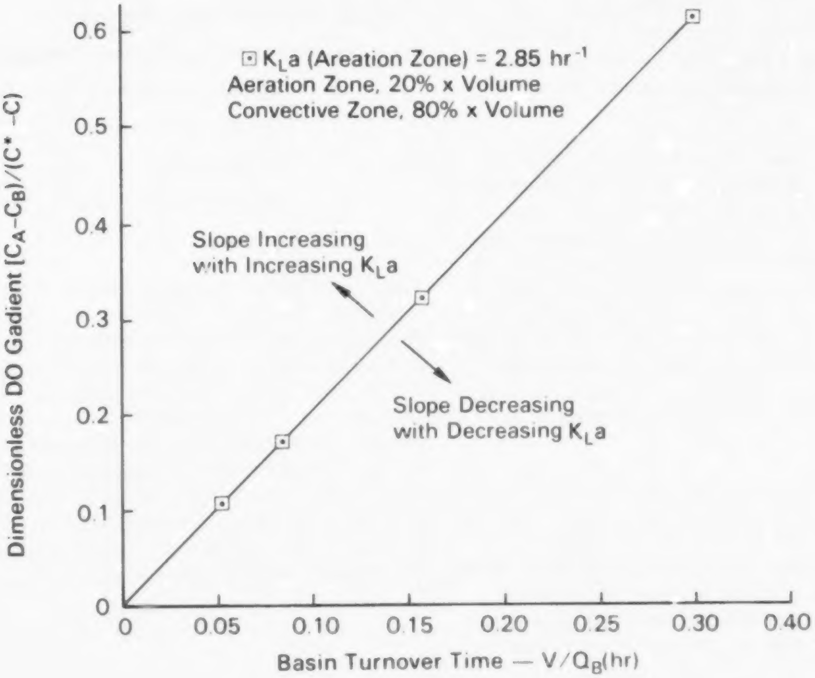


Figure 10. Dimensionless Dissolved Oxygen Gradient vs Turnover Time



dimensionless gradient group is equal to 0.30. When the bulk average concentration level reaches 20% of saturation, the observed gradient would be 24% of the saturation value. Similarly, as the 80% of saturation bulk concentration level is attained, this gradient is reduced to 6% of the saturation value. This convergence of gradients and manner of comparison is highly consistent with actual reaeration data, Figure 11. Thus, larger gradients are observed in early portions of a test when the oxygen uptake rates are high (large driving force region) while significantly smaller gradients appear as equilibrium is approached. It should be emphasized however, that the values presented are associated with the specific numerical example cited.

Applying perfect mixing concepts to a reaeration test generally requires that dissolved oxygen gradients be nonexistent during all portions of the test. For any real test situation, however, these gradients do and must exist since the aeration transfer process is generally localized and must be dispersed through the basin via the bulk mixing mechanism. The two zone model reiterates that dissolved oxygen gradients are inherent to the reaeration process. For a particular system or application, it becomes a judgement decision to define what constitutes a reasonable or acceptable dissolved oxygen gradient for operational purposes.

In order to observe whatever gradients exist, it is necessary to have at least four to six sample pumps distributed throughout the basin. At least two sample points should be within the lower third of the basin and two sample points in the upper third. While radial position is somewhat arbitrary, sampling in locations in the vicinity representing 30-40% and 70-80% of the tank volume should provide the essential data. It is desirable, but not mandatory, to have extra sample pumps located at mid-depth and other locations to augment these measurements.

Placement of the sample pumps as described in the previous paragraph should exemplify the complete range of DO gradients within the basin. Gradients in themselves are an inherent part of the reaeration process and are not necessarily indicative of any special problems. In order to eliminate these gradients, one would need to exert a significantly greater amount of energy to the mixing process. The most efficient aeration system is one which balances the mixing energy required to maintain acceptable DO gradients and solids suspension. The net result would be oxygen transferred and distributed at a minimum cost of energy.

Another indicator of mixing in a reaeration test is the bottom velocity. Gravitational forces irrepressibly drag the suspended solid particles to the bottom of the basin. A strong flow along the bottom will reentrain these particles and prevent any buildup of solids in the bottom of the basin. Hence, both magnitude and direction are important. It should be emphasized that bottom velocities alone are not sufficient to describe the hydraulic process within the tank. These measurements should be viewed in conjunction with the dissolved oxygen data.

Indirect indicators of mixing, i.e., turnover time, pumping capacity and power per unit volume do not in themselves assure adequate solids suspension. It is quite possible to have a vigorous flow pattern in the upper half of the basin with low flow rates along the bottom.

In subsurface systems, similar problems in mixing and mass transfer can result from improper gas dispersion or flooding conditions. (Gassed processes can also be susceptible to another problem, viz. flotation.)

A brief review of impeller characteristics provides clues to obtaining maximum pumping capacity at minimum power. For geometrically similar impellers operating in equivalent flow regimes (as indicated by Reynolds number, Froude number, etc.), the primary pumping capacity is given as:

$$Q_I = N_Q n D^3 \tag{8}$$

The equation for power is expressed as:

$$P = K_3 N_p n^3 D^5 \tag{9}$$

Maintaining a constant power, Equations 8 and 9 are solved for pumping as a function of impeller diameter:

$$Q_I = K_4 N_Q (P/N_p)^{1/3} D^{4/3} \tag{10}$$

where:

Q_I = primary pumping capacity (L^3/t)
N_Q = dimensionless flow number
n = impeller speed (L/t)
D = impeller diameter (L)
P = power (fL/t)
N_p = dimensionless power number
K_3, K_4 = constants

The result states that pumping is proportional to diameter to the four-thirds power. This flow refers only to the fluid being pumped directly through the impeller field. The flow from the mixing impeller entrains more fluid in the tank so that the total circulating flow can be several times greater than the value of Q_I obtained above. These results illustrate a distinct advantage to going to larger diameter impellers operating at lower speeds. This conclusion is consistent with observations from several mechanical aeration devices and are yet another manifestation of the interrelationship of mixing and aerator performance.

Performance in Waste Stream

One of the key problems addressed at this Workshop is the application of results from a clean water reaeration test to performance in a waste stream. The two are distinctly different processes each with its own unique traits. The latter is a steady state bio-chemical process, the bio-chemical kinetics of which are often difficult to assess precisely, especially on a large-scale. In addition, the characteristics of the biomass vary with time within a specific system and can be grossly different from system to system. The clean water reaeration test uses well defined chemical reaction and physical absorption processes in a non-steady state procedure.

As the goal is performance in the waste stream, the clean water reaeration test is only meaningful when the results are interpreted in this context. Design of an aeration system generally includes the following goals:

1. Oxygen transfer efficiency
2. DO levels above prescribed minimum
3. Solids suspension.

In a reaeration test, the physical processes associated with and correlated to these criteria are:

1. Mass transfer in the aeration zone
2. Convection of oxygen enriched water
3. Bottom velocities — magnitude and direction.

The correlation of these items, however, does not occur on a one-to-one basis. The waste stream is a different environment, and these differences require special attention.

An observed phenomenon which highlights these differences is shown in Figure 12. In wastes with a high mixed liquor volatile suspended solids, test results have suggested a biologically enhanced performance of aeration devices. That is, performance in the waste exceeded predicted performance from clean water. Several possible explanations of this phenomenon have been put forth, but the precise nature of this effect is still elusive.

A waste characteristic that often accompanies this enhancement is a decrease in the surface tension. For surface aeration, this decrease in surface tension appears to correlate with a secondary type of flow pattern as previously described in Figure 2. Therefore, it is possible for bottom velocity measurements in the clean water test to be acceptable and perhaps not achieve proper mixing requirements in the actual waste. Experiences of this nature tend to encourage some minimum testing in the waste stream. While a clean water reaeration test is preferred for demonstrating oxygen transfer capabilities, a simple test in the waste stream would be a much better indication of satisfactory solids suspension. Probing the basin bottom after several months of operation and obtaining grab samples at several different elevations within the basin is currently the only accurate means of assuring adequate solids suspension. This could also be artificially induced, with difficulty, in a clean water test through surface tension adjustment and inert solids addition. Rather than specifying unduly high clean water bottom velocities, it would be more relevant and meaningful to measure solids suspension directly in waste operation.

Full- or Small-Scale Tests

Extrapolating the velocity measurements made for one set of geometric conditions to a different set can only be done in an approximate fashion. The flow relations for aeration systems are quite complex and to date have not been analytically solved. (Some numerical evaluations have been performed on simplified systems.) Since both oxygen transfer and solids suspensions are dependent upon the flow patterns, the reaeration test basin should be geometrically similar to the waste basin.

The most critical geometric conditions for scaling are side water depth, sparge location, surface area and degree of baffling. A fully baffled tank is defined as one in which the baffle surface area is sufficient to remove the basin swirl component. This swirl component is induced by mechanical aeration devices which have a rotational motion. Similar types of motion can also be induced by submerged jets and other aeration devices with a preferred direction for induced motion. When possible, tank dimensions for the reaeration tests should be within ±10% of the desired waste basin configuration. If this condition cannot be satisfied, tests in basins beyond these limits require a substantial understanding of the controlling parameters. While the uncertainty in such testing increases, it is possible to provide a meaningful indication of aerator performance.

Model studies can be used only with the greatest of care. The process to model contains many diverse elements and in order to properly scale the complete process the contribution of each element must be examined. The elements include:

1. Local Mass Transfer — surface splash and spray, or bubble size, and hydrostatic head
2. Convection of Enriched Fluid — hydraulic flow patterns, bottom velocities.

Even if the scaling parameters were known precisely, it is difficult to satisfy all necessary requirements in a small-scale model. Reduced scale test results probably should be limited to visualization studies of hydraulic flow patterns and preliminary investigations. This type of testing could be useful for determining baffle configurations, bottom velocities and approaches to rectify specific flow patterns.

Figure 11. Typical Reaeration Test Data

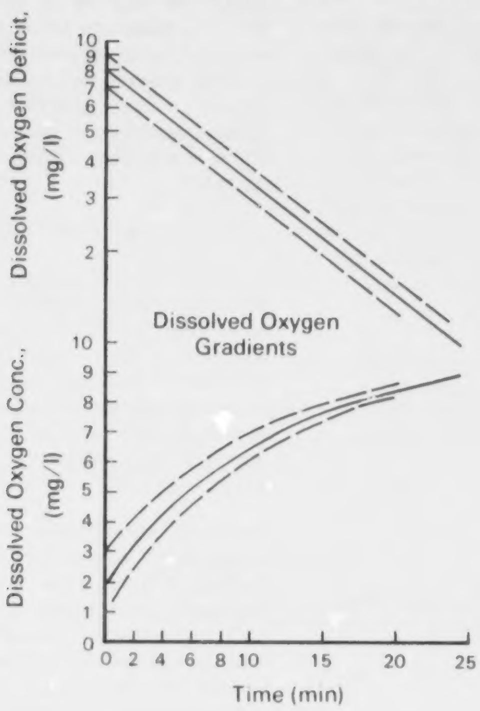
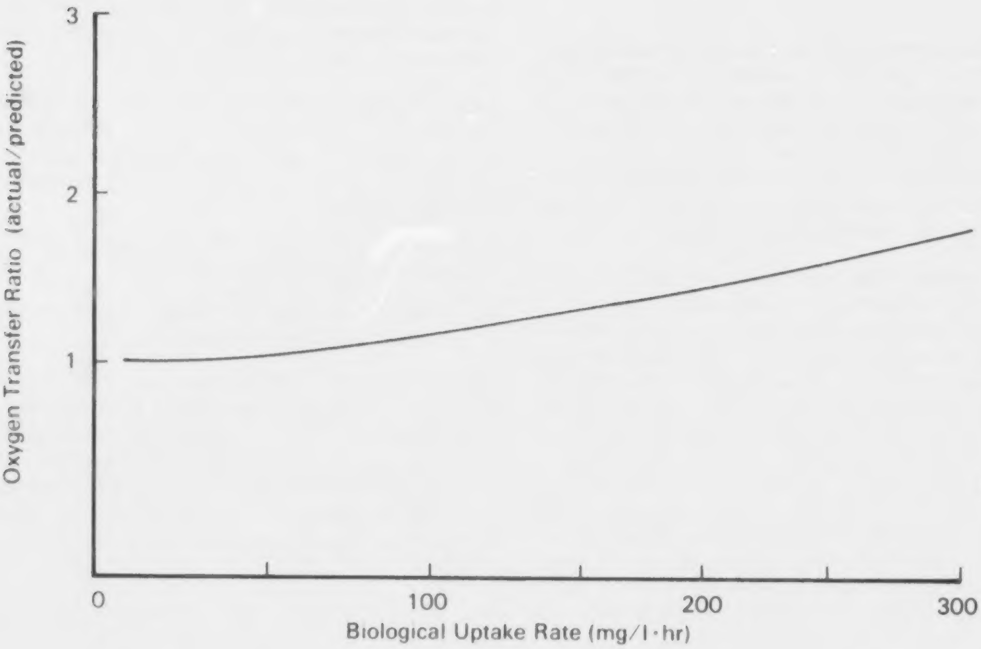


Figure 12. Biological Enhancement of Oxygen Transfer



Conclusions

1. As defined, the measured mass transfer coefficient is innately dependent on mixing.
2. A minimum of four to six sample points are required for clean water test measurements.
3. DO gradients are intrinsic to the reaeration test and do not necessarily imply problems in mixing or mass transfer.
4. Individual sample point uptake rates should agree closely with that based on average basin DO. Pronounced rate differences might indicate adverse gradients in the waste or improper blending.
5. Bottom velocities (magnitude and direction) are a good indication of solids suspension capabilities.
6. Turnover time, pumping capacity and power per volume are poor indicators of mixing and solids suspension, per se.
7. The best test of solids suspension is solids measurements in the actual waste stream.
8. Scaling of aeration processes is complicated. Tests should be run in a similar sized basin.

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Sampling Considerations

12

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Introduction

In the evaluation of aeration equipment, obtaining a representative samle is basic. Each phase of the evaluation, test procedure, data analysis, and data presentation, is important, but using a proper sampling method is essential. There are two methods commonly used for the evaluation of aeration equipment, namely the Clean Water Non-Steady State Method and the In-Process Method. The Clean Water Non-Steady State Method uses two test procedures: wet chemical analysis (Winkler) and the dissolved oxygen probe . The sampling methods suggested in this presentation will include both the Clean Water Non-Steady State Method and the In-Process Method.

Clean Water Non-Steady State Method

General Considerations

The basic mass transfer equation:

$$dC/dt = K_La\,(C^*-C)$$

requires that mixing be complete. (Complete mixing is defined here as the continuous movement of all liquid in the basin.) The first consideration, therefore, in any aeration device evaluation is to assure that mixing is complete.

One test for complete mixing that has been suggested is that the dissolved oxygen concentration, C, be uniform (not vary more than ±0.25 mg/l) throughout the basin. This is not a valid indicator of uniform or complete mixing. Since all aeration devices are point sources of oxygen transfer, there must be an oxygen concentration gradient across the aeration device. If not, the device is simply not transferring oxygen. For example, a low speed surface aerator will have an oxygen concentration gradient of 4 to 5 mg/l between the inlet liquid and the aerator issue. Therefore, there must be an oxygen gradient in the aeration basin. Using a uniform oxygen concentration as an indication of complete or uniform mixing is not technically sound.

Using oxygen saturation as an indication of completely mixed conditions is also not technically sound. Due to the varying pressure conditions in a basin, oxygen saturation can and will vary somewhat with liquid depth. This is true even for surface aerators where mixing is intense, causing secondary aeration (bubble entrainment) to occur at the walls of the basin. The representative value of oxygen saturation for any aeration system is difficult, if not impossible to establish. Although this problem has been discussed at length, a satisfactory conclusion has not been reached.

The author prefers to use the final factor in the mass transfer equation to establish if a completely mixed condition is achieved. That factor is the overall mass transfer coefficient, K_La. If the aeration system is truly mixed, the value of K_La will be constant within the range of experimental error. After reviewing the results of several hundred clean water non-steady state tests, it was established that the value of K_La should not vary more than ±7.5% due to testing variations. If the value of K_La varies more than ±7.5%, the test procedure should be checked thoroughly. If no apparent problems are found, then the test results should be rejected on the basis of incomplete mixing.

To establish the variation in K_La, at least four sampling points should be used. These sampling points should be placed randomly in the basin avoiding the areas near the aerator issue and near the walls. The data from each sample point should be handled separately (do not average the dissolved oxygen concentrations) to establish the value of K_La. If a single point varies greatly from the remaining points, check the location to determine if it is in a zone of intense aeration or low turbulence (behind a column or baffle).

Sample Location

Concerning the location of sampling points, only a single point is needed once completely mixed conditions are established. If only one sample point is used during general testing, it should be located at the inlet to the aeration device. For example, for diffuser aeration devices, the sample point should be located at the same elevation as the diffusers and one foot from the nearest air release point. For jet aeration devices, it should be located at the inlet to the pump.

If it is desired to have a number of sample points, then the following criteria are suggested:

1. Do not place sample points in the plume or issue of the aeration device.
2. Do not place sample points any closer than three feet from the walls, six feet from the corners, or two feet from the basin floor and liquid surface.
3. Place one or more sampling points near the liquid inlet to the aerator.
4. Place sampling points at the one-third and two-thirds points in the basin both in relation to the diameter and depth. The diameter points should be on opposite sides of the basin.

When using multiple sampling points, in no case should the oxygen concentration data be averaged for each time increment. It is mathematically improper to average dissolved oxygen values at each time increment to obtain an average K_La value. Therefore, the data from each sample point must be handled independently to establish separate values of K_La. Then individual K_La values may be averaged.

Sample Volume and Rate

For the wet chemical analysis (Winkler), the sample volume (about 300 ml) used is based on the standard BOD_5 bottle. The sampling procedure should be to place the sampling points (pumped or siphoned) in their proper locations in the aeration basin. Plastic tubing (about one-quarter inch diameter) should be connected from the sample point to a central collection point. At timed intervals, the plastic tubes are placed into the BOD_5 bottles such that the ends of the tubes are within one-half inch of the bottle bottom.

The rate of sample flow should be such that the bottle is filled within 5 sec. This means that the bottle contents will be replaced a minimum of three times in 15 sec.

The length of the sample lines (for more than one sample point) is not critical if the retention time in the tube is less than 7.5 sec. If the retention time is greater than 7.5 sec, then all sample lines should be of the same length. To avoid possible data bias, the maximum sample line retention time should be 7.5 sec or less, if possible. Longer retention times may result in liquid temperature changes and possible additional aeration where air bubbles may be drawn into the sample line.

Sample Point Configuration

To avoid the inclusion of air bubbles into the sample line, the sample point should be located away from areas of intense turbulence. The inlet to the sample point should be in a vertical plane with the opening at the top. The downward inlet velocity should be such that all air bubbles larger than 500 microns are excluded.

Sample Timing

Timed samples are collected from each sample point. At least ten samples should be collected at each point for dissolved oxygen concentration analysis. Seven or eight of these samples should have an oxygen concentration between 10 and 90% of saturation. In order to achieve the appropriate time interval, the following expression can be used as a first estimate.

$$t = 100M/OTR$$

where:

t = sample time interval, min
M = test basin water weight, lb × 10^{-6}
OTR = estimated oxygen transfer rate, lb O_2/hr

The following example illustrates use of the above expression.

Aerator - 100-hp low speed surface aerator
Basin Dimensions - 60′ × 60′ × 15′ SWD
Estimated Efficiency - 3.0 lb O_2/hp·hr

$OTR = 100\,(3.0) = 300$ lb O_2/hr
$M = 60\,(60)\,(15)\,(62.4) = 3.37 \times 10^{6}$ lb
$t = 100\,(3.37)/300 = 1.1$, use 1.0 min sample times

Data Variation

A major problem sometimes associated with sampling data is noise or scatter. It has been the author's experience that data scatter or noise increases with increasing energy input. The first reaction noted by most observers to data scatter for clean water non-steady state testing is that mixing is incomplete or insufficient. However, as noted above, data scatter becomes most severe at the highest input energy level.

A possible explanation of the above noted observation is the following:

1. At low input energy levels, the flow pattern in the aeration basin is uniform, nearly laminar in nature.
2. For uniform or laminar flow conditions, the data collected are uniform, low in scatter and high in correlation.
3. As the energy input level reaches some critical level, uniform or laminar flow conditions are lost and turbulent conditions are established. With increasing turbulence, the data collected at any point in the basin become more scattered and noisy.

An example of the above conditions was observed recently in a field clean water non-steady state test on low speed aerators. In this case, four 75-hp low speed aerators were placed in a 100-ft square basin. Six sampling points were located randomly in the basin. The individual log deficit plots for each sampling point correlated poorly at full power (75-hp for each unit). Comparison of the six resulting K_La values showed a relatively wide variation. However, the mixing was very intense as the entire basin was white with air bubbles that had been entrained throughout the basin volume. It was suggested that the reason for the poor correlation and scatter was due to "poor" mixing conditions.

The above testing was completed on two speed units having a 10-inch liquid submergence variation. The entire range of power levels, from 75-hp down to about 25-hp, was evaluated. It was interesting to note that as the input power was reduced, the data tended to correlate better with less scatter.

In the above example, data scatter or noise was associated with mixing intensity. This same observation has been noted for bottom velocity measurements. In the case of bottom velocity, it was observed that the bottom velocity increased in proportion to energy input up to some critical value. At that value and beyond, the bottom velocity began to decrease. The critical input energy level appeared to be the point between uniform and turbulent mixing conditions.

Dissolved Oxygen Probes

Dissolved oxygen probes have been used extensively for the evaluation of aeration devices. The primary motivation for the use of DO probes is the ease of data collection. Certainly, when the probes are applied and function properly, their use is acceptable. However, assurance that the probes are used properly and are functioning properly is difficult to achieve and can be time consuming.

The first problem with DO probes is their electronic nature. The sensor and meter are subject to environmental conditions such as temperature, pressure, and moisture. All of these factors affect the operation of the unit.

Secondly, being electronic, most technical people are not competent to operate and maintain the unit so that proper results are assured.

Thirdly, the unit is fragile, requiring care to assure that the membrane is not punctured or broken during testing. In many tests conducted by the author, the membrane was found faulty during the testing.

Finally, probes do not indicate a finite value of oxygen concentration. Since time is required for oxygen to diffuse through the membrane, the indicated oxygen concentration is an average value over a period of time. This is one reason why DO probe data are more uniform and correlate better than wet chemical analysis (Winkler) data.

In-Process Method

Sampling is required to establish several parameters utilized in the evaluation of aeration equipment using the in-process test procedure.

Alpha

Alpha is the ratio of the wastewater K_La to the clean water K_La. It is determined in the laboratory with an aeration device which is similar to that used in the full-scale operation. Alpha is the correction for surfactants that may be present in the wastewater. It is the author's opinion that alpha values should be based on a sample of the mixed liquor with the solids present and the microorganisms killed using a sulfamic acid-copper sulfate solution. Other samples such as the influent, effluent, or settled mixed liquor do not represent the actual operating conditions.

Beta

Beta is the correction for salinity. A sample of the mixed liquor should be filtered through a membrane filter. The filtrate should be then analyzed for total dissolved solids. The saturation value for the wastewater is then corrected for salinity, represented by the total dissolved solids.

Residual Dissolved Oxygen, C

It is commonly held that an oxygen profile should be completed on the aeration basin contents to establish the value of C, the basin dissolved oxygen concentration. It should be recalled that the purpose of the test is to evaluate the aeration equipment and not the biological process. What does the aerator "see"? The aerator does not "see" the average basin oxygen concentration. The aerator "sees" only the liquid entering the device. Therefore, the sampling point for determining C should be at the inlet to the aeration device and not an average basin dissolved oxygen based on a DO profile.

Oxygen Uptake Rates, R

The oxygen uptake rate is a direct measurement of the microbial utilization of oxygen. If it has been established that the aeration basin is completely mixed, then three or more samples can be taken at any point in the aeration basin. However, since a relatively high concentration of dissolved oxygen is required in the sample for the determination of the uptake rate, it is suggested that three or more samples of the aerator issue be taken for analysis.

If complete mixing of the basin is questioned, then samples should be taken from at least ten random locations for oxygen uptake analysis. Completely mixed conditions are indicated if the variation of oxygen uptake is ±10% or less.

Liquid Temperature

For completely mixed conditions, one sample point is all that is required. That sample point should be at the inlet to the aeration device.

In-Process Formulas

Overall Mass Transfer Coefficients:

$$(K_La)_{20} = [R/\alpha(\beta C_Y^* - C)]\theta^{(20-T)}$$

Oxygen Transfer Rate:

$$\mathrm{SOTR} = (K_La)_{20}(C_Y^*)M$$

$$\mathrm{OTE} = [\mathrm{SOTR}/Q_a(1.035)]$$

where:

R = oxygen uptake rate, mg/l·hr

α = $(K_La)_{20}$ wastewater/$(K_La)_{20}$ clean water

β = oxygen saturation correction value for salinity

C_Y^* = clean water oxygen saturation at the inlet to the aeration device, mg/l

θ = temperature correction factor

$(K_La)_{20}$ = overall mass transfer coefficient for standard conditions, hr^{-1}

T = basin liquid temperature, °C

M = basin water weight, lb × 10^{-6}

SOTR = standardized oxygen transfer rate in clean water, lb O_2/hr

OTE = oxygen transfer efficiency, dimensionless

Q_a = air flow rate, scfm

Conclusion

Sampling is a key element to be considered when evaluating aeration equipment. The following questions should be answered before an acceptable sampling arrangement is approved.

What is the:

Sampling location?
Sampling volume?
Sampling rate?
Sample configuration?
Sample source?
Sample time?

It should be remembered that the basis of all aeration testing is the mass transfer equation. The basic restraint in the use of the mass transfer equation is that the aeration basin must be completely mixed.

Analytical Measurement and Saturation Values for Dissolved Oxygen in Water

13

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Introduction

Solubility of oxygen in water is a relatively straightforward concept which is accepted primarily on the basis of facts which involve the composition of the gas phase, the composition of the water phase and Henry's Law. Technical and engineering practices utilize values for oxygen saturation in water as a basis for driving forces in the transfer of oxygen from gaseous to liquid phases. Oxygen transfer calculations must begin with a known entity. Oxygen saturation values are available from literature and references sources and are convenient for use.

The available information on oxygen saturation of water from air was obtained under carefully controlled conditions where the air temperature and the water temperature were the same and the air was saturated with water vapor (in most cases). When the "saturation value" is applied across a relatively broad set of conditions which involve differences in air and water temperature and less than saturated conditions for water vapor in the air phase, the applicability of the information may be questioned. The following discussion is an examination of some of the factors involved in oxygen saturation and some of the questions both answered and unanswered.

Oxygen Saturation of Water From Air

Definition

The examination of facts and questions should begin with a definition of oxygen saturation of water. Saturation is the result, measured as molecular oxygen concentration in the water phase, of an equilibrium state in which molecular oxygen in the air phase diffuses both into and out of the water phase at the same rate. The most important factor in this definition of saturation is that it assumes an equilibrium state, and therefore, the results of oxygen saturation of water would be shifted by any factor which shifted the equilibrium.

Examination of Information Available

Whipple & Whipple (8) published in 1911 information on solubility of oxygen in water and sea water based on observations by Fox (3) (4) in 1907 and 1909. The published information by Whipple & Whipple is the basis for information which is presented in the 14th Edition of "Standard Methods for the Examination of Water and Wastewater" (7). In 1966, Carpenter (1) published the results of studies of oxygen solubility in water and those results differed somewhat from the information published by Whipple & Whipple. The information developed by Carpenter (Table 1) shows somewhat lower values of oxygen saturation than that published by Whipple & Whipple. The difference is greatest at high temperatures where it reaches approximately 0.2 mg/l.

Table 1. Oxygen Solubility (mg/l) in Water, Comparison of Available Information

Temperature (°C)	Whipple & Whipple (8) 1911	Carpenter (1) 1966
0	14.6	14.6
2	13.8	13.8
4	13.1	13.1
6	12.5	12.4
8	11.9	11.8
10	11.3	11.3
12	10.8	10.8
14	10.4	10.3
16	10.0	9.9
18	9.5	9.5
20	9.2	9.1
22	8.8	8.7
24	8.5	8.4
26	8.2	8.1
28	7.9	7.8
30	7.6	7.5
32	7.4	7.3
34	7.2	7.1
36	7.0	6.8
38	6.8	6.6
40	6.6	6.4

Carpenter concluded that the work by Fox potentially represented some degree of supersaturation at lower temperatures, but the differences at higher temperatures were possibly due to working from cooler to warmer conditions with inadequate time for equilibrium to be established. Work on the solubility of nitrogen in sea water by Rakestraw & Emmel in 1938 indicated that the observations by Fox appeared to be too high by about 1%. The magnitude of differences between the observations of Fox and the observations of Carpenter are not alarmingly large, but the differences do warn that the equilibrium state for oxygen solubility in water is not easily attained and that supersaturation is certainly possible. As a matter of interest, oxygen solubility information based on the observations of Carpenter are presented in Tables 2 and 3.

Factors Involved in Oxygen Saturation

Beginning with Henry's Law, the concentration of oxygen molecules in the gas phase is a key factor. Since air is a compressible fluid, the number of oxygen molecules in a given volume will be directly proportional to the absolute pressure and indirectly proportional to the absolute temperature. If we apply this concept to the air-water interface (Figure 1), the concentration of oxygen per unit area at the interface will be directly proportional to pressure and indirectly proportional to absolute temperature.

If we consider the film of air immediately adjacent to the water interface, the concentration of oxygen present is further adjusted by the concentration of water vapor molecules present. Since the total pressure in the gas phase is made up of the partial pressures of the gases present, the water molecules are present in proportion to the partial pressure corresponding to the vapor pressure of the water at the water temperature.

Once the oxygen molecule has migrated into the water phase, the assumption must be made that the oxygen molecules are diffused and/or mixed throughout the water phase on a relatively homogenous basis. The tendency of the oxygen molecule to remain in the water phase rather than escape immediately back into the gas phase is dependent upon molecular attractions between water molecules and oxygen molecules. The equilibrium state of the molecular attraction depends upon the kinetic energy of the water and oxygen molecules and upon impurities in the water which also associate with the water molecules and influence the overall status of molecular attraction.

Therefore, three significant factors in oxygen saturation of water are:

1. The concentration of oxygen in the gas phase (particularly at the air-water interface)
2. Forces of molecular attraction between water and oxygen molecules in the water phase
3. Kinetic energy of water and oxygen molecules in the water phase.

Concentration of Oxygen Molecules in Air at the Air-Water Interface

The factor of pressure (and oxygen concentrations) of the gas phase is easily understood where there is a simple interface between air and water and when the gas phase is under atmospheric pressure or some other applied pressure. The situtation is more complex when there is a mechanical generation of interfaces between air and water such as results from bubble diffusion or turbines. Since the air in bubble form is then mixed into the water, pressure resulting form weight of water is added to atmospheric pressure.

For any particular device which is employed for the generation of air-water interfaces, knowledge of the air-water interface in terms of area of interface and distribution of the interfaces in the water system is not readily available or modelable; therefore, pressure and the corresponding concentration of oxygen molecules at the generated interfaces are unknown. It is a practical recommendation that each oxygen transfer system must be considered unique and that the apparent oxygen saturation value for the system must be determined experimentally. The system which involves air and water interfaces in the water depth of the system will reach an "equilibrium condition" which is supersaturated with respect to atmospheric pressure. At "equilibrium saturation" the system may transfer oxygen into water in one zone and transfer oxygen out of water in another zone. Superimposed over the transfers may be a significant tendency toward supersaturation. It seems practical that the observed equilibrium state for that system is real, and the observed saturation value should be utilized in defining the driving force for oxygen transfer in the particular system.

The consideration of water vapor at the air-water interface is straightforward when the air and water are at the same temperature and the air is saturated with water vapor. If the conditions are shifted so that the air is not at the same temperature of the water and is not saturated, is the concentration of molecules at the air interface altered? Is the equilibrium which represents oxygen solubility into water shifted? If it is assumed that the interface is at the temperature of the water, then water temperature would control the vapor pressure in the gas phase at the interface and the concentration of oxygen molecules at the interface (gas film) would be controlled by the temperature of the water. Air temperature and relative humidities would not be a factor.

When the air is dry, evaporation of water occurs. Evaporation requires heat energy which is drawn from the water phase at the interface. Therefore, it is logical to assume that the interface is being cooled and is not at the same temperature as the body of the water. If the water at the interface is cooled, the vapor pressure of water at the interface would be decreased and the concentration of water molecules in the gas layer at the interface could be less than that predicted from the temperature of the body of water. Additionally, if the air film at the interface is cooled by evaporation, the density of air at the interface could be increased and would not be represented by Henry's Law for the gas mass. Both

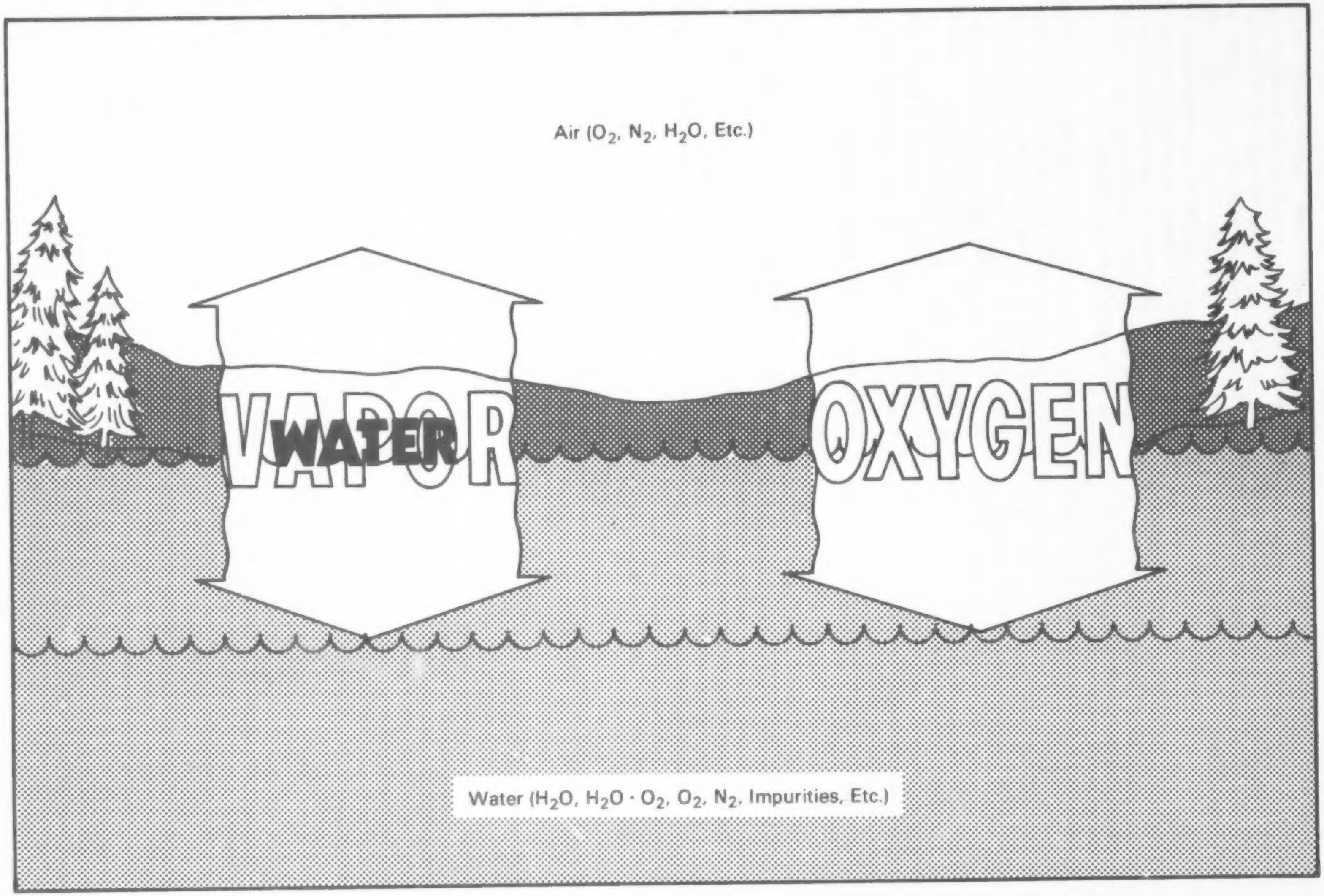
Table 2. Solubility of Oxygen (mg/l) at Various Temperatures and Elevations
(Based on Sea Level Barometric Pressure of 760 mm Hg)

Temperature (°C)	Elevation (Feet Above Sea Level)						
	0	1000	2000	3000	4000	5000	6000
0	14.6	14.1	13.6	13.2	12.7	12.3	11.8
2	13.8	13.3	12.9	12.4	12.0	11.6	11.2
4	13.1	12.7	12.2	11.9	11.4	11.0	10.6
6	12.4	12.0	11.6	11.2	10.8	10.4	10.1
8	11.8	11.4	11.0	10.6	10.3	9.9	9.6
10	11.3	10.9	10.5	10.2	9.8	9.5	9.2
12	10.8	10.4	10.1	9.7	9.4	9.1	8.8
14	10.3	9.9	9.6	9.3	9.0	8.7	8.3
16	9.9	9.6	9.2	8.9	8.6	8.3	8.0
18	9.5	9.2	8.9	8.6	8.3	8.0	7.7
20	9.1	8.8	8.5	8.2	7.9	7.7	7.4
22	8.7	8.4	8.1	7.8	7.6	7.3	7.1
24	8.4	8.1	7.7	7.6	7.3	7.1	6.8
26	8.1	7.8	7.6	7.3	7.0	6.8	6.6
28	7.8	7.5	7.3	7.0	6.8	6.6	6.3
30	7.5	7.2	7.0	6.8	6.5	6.3	6.1
32	7.3	7.1	6.8	6.6	6.4	6.1	5.9
34	7.1	6.9	6.6	6.4	6.2	6.0	5.8
36	6.8	6.6	6.3	6.1	5.9	5.7	5.5
38	6.6	6.4	6.2	5.9	5.7	5.6	5.4
40	6.4	6.2	6.0	5.8	5.6	5.4	5.2

Table 3. Solubility of Oxygen (mg/l) at Various Temperatures and Chlorinity
(Based on Sea Level Barometric Pressure of 760 mm Hg)

Temperature (°C)	Chlorinity (%)					
	0	4.0	8.0	12.0	16.0	20.0
0	14.6	13.9	13.2	12.5	11.9	11.3
2	13.8	13.2	12.5	11.9	11.4	10.8
4	13.1	12.5	11.9	11.3	10.8	10.3
6	12.4	11.8	11.3	10.8	10.3	9.8
8	11.8	11.3	10.8	10.3	9.8	9.4
10	11.3	10.8	10.3	9.8	9.4	9.0
12	10.8	10.3	9.8	9.4	9.0	8.6
14	10.3	9.9	9.4	9.0	8.6	8.3
16	9.9	9.4	9.0	8.6	8.3	8.0
18	9.5	9.1	8.7	8.3	8.0	7.6
20	9.1	8.7	8.3	8.0	7.7	7.4
22	8.7	8.3	8.0	7.7	7.4	7.1
24	8.4	8.1	7.7	7.4	7.1	6.9
26	8.1	7.8	7.5	7.2	6.9	6.6
28	7.8	7.5	7.2	6.9	6.6	6.4
30	7.5	7.2	7.0	6.7	6.4	6.2
32	7.3	7.0	6.7	6.5	6.2	6.0
34	7.1	6.8	6.5	6.3	6.0	5.8
36	6.8	6.6	6.3	6.1	5.8	5.6
38	6.6	6.4	6.1	5.9	5.6	5.4
40	6.4	6.2	5.9	5.7	5.4	5.2

Figure 1. Air/Water Interface


Air (O_2, N_2, H_2O, Etc.)
VAPOR
WATER
OXYGEN
Water (H_2O, $H_2O \cdot O_2$, O_2, N_2, Impurities, Etc.)

factors could result in an increased concentration of oxygen molecules at the air-water interface, which would result in a higher saturation value for the body of water than predicted from body of water temperature. The opposite could be projected for conditions of condensation.

In a experimental study completed in 1971 (unpublished), the relationship of water temperature, air temperature and relative humidity were examined in the range of 5 to 35°C. Water temperatures were selected at 5, 15, 25 and 35°C, and for each water temperature, the air temperature was established at a wet bulb temperature of 5, 15, 25 and 35°C. For each wet bulb temperature, the dry bulb temperature was established as equal to the wet bulb temperatue or 5, 10 and 15°C greater than the wet bulb temperature. Throughout the experimental program, air velocity at and parallel to the interface was maintained at approximately 5 mph. Through the entire range of temperature and relative humidity conditions, there was no indication that the saturation value of the water phase was significantly altered (more than 0.1 mg/l) by the air temperature and air humidity.

If the indicated experimental observations were conducted at greater air velocities (>20 mph), the degree of turbulence established in the air flow would be increased significantly. If the turbulence increased the rate of evaporation, the increased rate of removal of water vapor and heat from the interface might significantly alter the equilibrium of oxygen saturation. An answer to this question is yet to be obtained in a controlled experimental study.

Molecular Attraction Between Oxygen and Water Molecules

The concept of molecular attraction between the molecules in the water system and the oxygen molecule is important since changes in the degree of attraction would influence the equilibrium state of oxygen saturation in the water system. Interestingly, the saturation of oxygen in water correlates rather closely with water viscosity over the temperature range of 0 to 100°C (Figure 2). Thus, the molecular attraction between water molecules which influences viscosity and the molecular attraction between water molecules and oxygen molecules are interrelated. This correlation does not establish that an alteration of water viscosity will have an impact on oxygen solubility, but such could be the case if decreased or increased viscosity were the result of decreased or increased molecular attraction.

When inorganic salts are added to water, the concentration of oxygen at saturation is decreased. Presumably this effect occurs because the dissolved inorganic solids are associated with the water molecules and the result is a decreased molecular attraction between water molecules and oxygen molecules. The magnitude of the decreased oxygen concentration at saturation is shown in Figure 3 for selected salts. The correction factor in the plot of Figure 3 is a ratio of dissolved oxygen concentrations at saturation in the salt solutions divided by dissolved oxygen concentrations at saturation for distilled water. Thus, the ratio is the beta factor. The data in Figure 3 illustrate that very significant concentrations of dissolved salts are required before a pronounced impact on the beta factor is observed. The range for the selected salts is 0.1 to 0.3% (1,000 to 3,000 mg/l) before a 1% reduction in the beta factor is observed.

More extensive data concerning oxygen solubility in sea water are presented in Table 3 and are correlated with the chlorinity of the sea water. As a matter of interest, the solubility of oxygen in synthetic sea water, such as that defined in ASTM Standard Specification D-1142-52, is greater than the solubility of oxygen in natural sea water. The difference is approximately 1 mg/l at 20°C. Presumably the difference is due to a lower level of molecular attraction in the natural sea water.

Measurement of Oxygen in Water

Winkler Titration

The accuracy and applicability of the Winkler titration for chemical measurement of oxygen in water is well documented. The precautions necessary in the presence of oxidizing or reducing materials is understood, and methodology exists which can be used to accommodate these interferences.

The application of Winkler titration to the measurement of oxygen in clean water during oxygen transfer studies is definitely a practical application. An analytical problem which has been identified is related to the use of a cobalt catalyst in the sulfite deoxygenation procedure and the resulting precipitation of a cobalt compound (5). Upon acidification in the Winkler titration procedure, the cobalt precipitate can cause the liberation of an additional amount of iodine. Thus, the interference results in an oxygen concentration value higher than the actual.

Work by Mixing Equipment Company has shown that the magnitude of the cobalt compound interference may be related to hardness of the water and is eliminated if the pH of the water is made low enough to keep the cobalt compound in solution.

The basic precaution is to either utilize a low concentration of cobalt so that interference in the Winkler titration is insignificant, or if higher concentrations of cobalt are to be utilized, determine oxygen by other procedures such as the use of dissolved oxygen probes.

The Winkler titration procedure has adequate opportunities for error. Potential errors to be considered are:

1. Air oxidation of iodide
2. Volatilization of iodine

Figure 2. Oxygen Solubility in Water vs Viscosity (0° to 40°C)

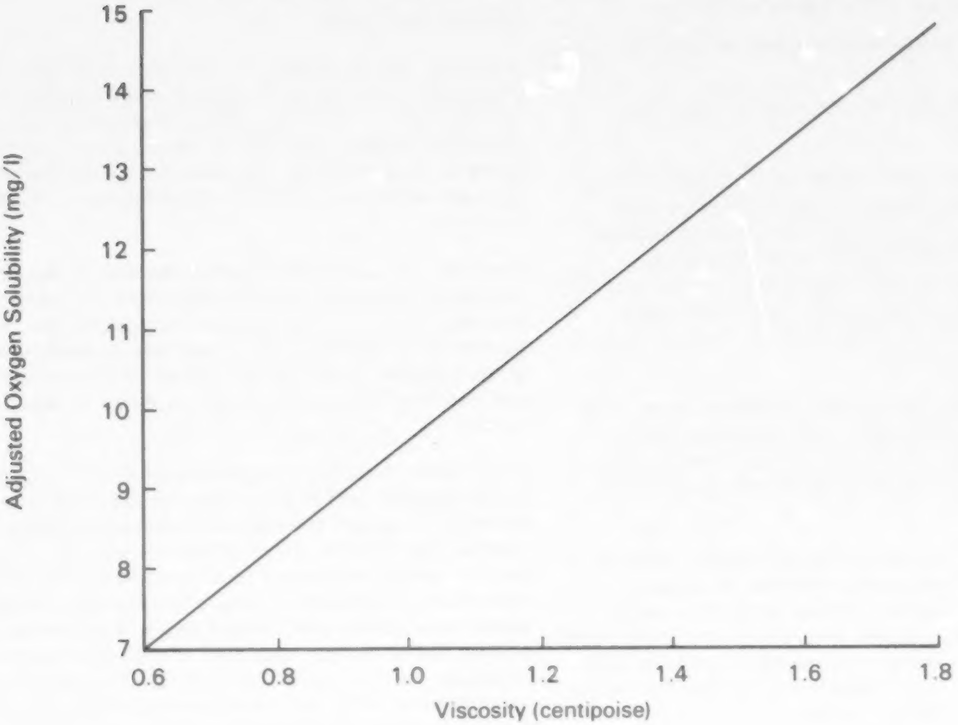
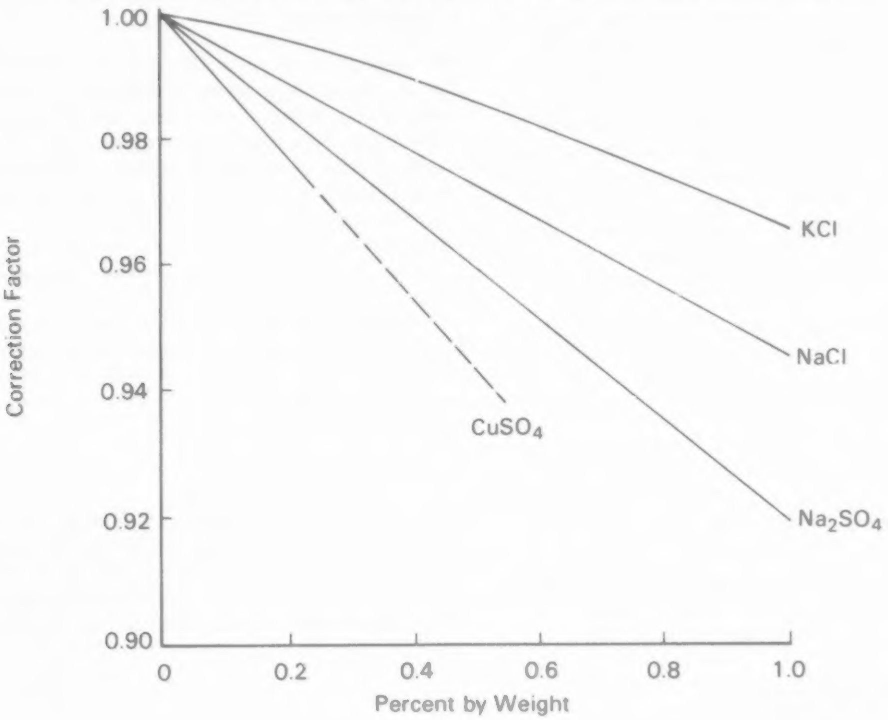


Figure 3. Effect of Dissolved Salts on Oxygen Saturated Water (22°C)



3. Oxygen contributed by the reagent solutions
4. Iodate contamination of the iodide solutions
5. Consumption or production of iodine by reagent contaminants
6. Difference between titration end point and the equivalency point.

Carpenter (2) found in work published in 1965 that the more significant errors were potentially from reagent blank errors and volatilization of iodine during titration. To reduce volatilization of iodine, he suggested an alkaline iodide reagent containing 600 g/l of NaI and 320 g/l of NaOH. The higher concentration of NaI would minimize the volatility of iodine through the formation of triiodide.

In earlier work published in 1965, Montgomery et al (6) concluded that iodine volatility was a problem and recommended the use of Pomeroy-Kirschman alkaline-iodide reagent. This reagent contains 400 g/l of NaOH and 900 g/l of NaI.

Standard Methods (7) does not identify iodine volatility to be a problem in the iodometric methods for oxygen determination. It does offer a choice of alkaline-iodide reagent, but does not suggest why one or the other might be selected. Alkaline-iodide reagent No. 1 contains 135 g/l of NaI. Alkaline-iodide reagent No 2 is the Pomeroy-Kirschman reagent (900 g/l of NaI).

The use of Standard Methods reagent No. 1 could lead to low results because of volatilization of iodine. The magnitude of the error would depend upon the analyst. Adoption of transfer procedures which reduce agitation and air contact with the sample followed by quick titration to near the endpoint would minimize iodine loss. If a less experienced analyst poured the sample from a BOD bottle into a graduated cylinder, poured the sample from the cylinder into an Erlenmeyer flask and proceeded with a slow titration, a very significant loss of iodine would occur. For warm samples (temp. > 20°C), the error could be significant, and at 30 to 35°C the results for a saturated sample could be low by as much as 0.5 mg/l.

The problem of iodine volatility can be suitably reduced by the following actions:

1. If Standard Method's alkaline-iodide reagent No. 1 is to be utilized, add the manganeous sulfate and alkaline-iodide reagents, carry the procedure through the flocculation stage and cool the sample to a temperature in the 10-15°C range before adding the acid reagent. Titrate the sample with due consideration for the volatility of iodine.
2. Use Standard Method's alkaline-iodide reagent No. 2 so that iodine is present as triiodide and iodine volatility is not a significant problem.

Dissolved Oxygen Probes

Membraned Probes

Dissolved oxygen probes are convenient for relatively precise determinations of oxygen, and as stated there is no interference from the cobalt compound which interferes with Winkler titration. To insure that a membraned probe is used correctly, it is well to understand the principle which is involved in the detection of oxygen by the probe.

Basically the membraned probe responds to the arrival of molecular oxygen at the cathode where it is converted to hydroxyl ions. Hydroxyl ions are correspondingly consumed at the anode in the conversion of elemental metal to the metallic oxide. The overall reaction establishes an electron flow which can be measured as an electrical current.

Precautions in using a membrane probe are not related to the reaction within the probe, but are related to the diffusion of oxygen through the membrane before it reaches the cathode. When placed in a water sample, the membrane is exposed to a concentration of oxygen molecules at the water-membrane interface. When the water is saturated with respect to air, the number of oxygen molecules per unit area at the water-membrane interface is the same as the number of oxygen molecules per unit area at the air-water interface (Figure 4). Thus, when the probe is in the saturated water solution, the opportunity for oxygen molecules to diffuse through the membrane is the same as exists when the probe is exposed directly to air.

The basic precaution which must be understood in the use of a membrane probe is that any contaminant in the water which reduces the molecular attraction between water molecules and oxygen molecules will reduce the concentration of dissolved oxygen at saturation in the water sample; however, at saturation the response of a membraned probe will be identical in clean or contaminated water and independent of oxygen concentration. To put it another way, a membraned probe cannot be calibrated in clean water and then placed in a contaminated water sample to read an oxygen concentration to be used in the determination of a beta value. For the oxygen probe to be utilized, it must be calibrated in the contaminated sample utilizing some reference technique such as Winkler titration.

Direct Contact Probes

Probes which provide a direct electrode contact with the sample do not have the same calibration precaution as related to the probes which utilize a membrane. The oxygen arrives directly at the electrode in proportion to the concentration of molecular oxygen in the water

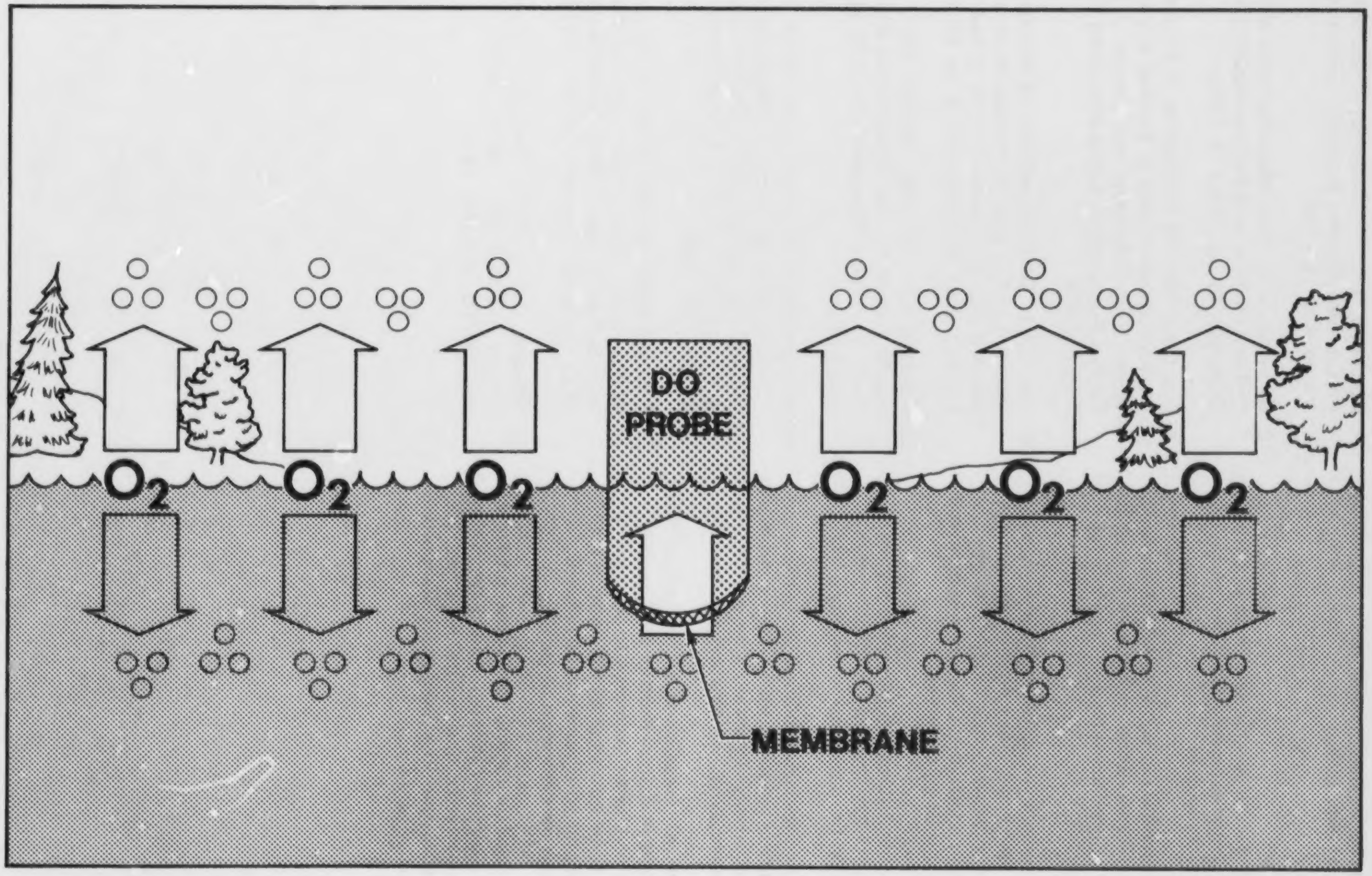

DO PROBE
MEMBRANE
O_2
O_2
O_2
O_2
O_2
O_2

Figure 4. Membraned Dissolved Oxygen Probe Response in Water at Saturation

sample. Where other problems such as fouling of the electrode surface and electrolyte concentrations are not a factor, a direct contact probe would be more suitable for the determination of beta values.

Summary

1. Excellent information exists for saturation of oxygen in water, but direct application in calculation of driving forces in oxygen transfer is subject to question.
2. Since oxygen saturation in water is a dynamic equilibrium, the concentration value could be influenced by conditions at the interface such as evaporation or condensation.
3. Controlled experiments at air velocities of 5 mph did not reveal any significant effect on the saturation value of oxygen in water over a range of evaporation and condensation conditions. Different results might be found at increased air velocities.
4. Where air-water interfaces are purposely dispersed in an oxygen transfer system, it is recommended that oxygen saturation be determined experimentally.
5. In addition to the interface condition (oxygen concentration), solubility of oxygen is influenced by molecular attraction. Thus, dissolved salts or other agents which alter molecular attraction can alter oxygen solubility.
6. Winkler titration may be affected by interference from cobalt precipitates in oxygen transfer studies, but iodine volatility can be an even greater source of error if interference is not corrected by the titration technique or by utilizing an alkaline-iodide reagent containing a high concentration of iodide.
7. Membraned probes respond to the "partial pressure" of oxygen and require a reference calibration in contaminated water in order to measure the actual concentration of oxygen. The precaution is important in determination of beta values.

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14

Accounting for the Effects of Water Temperature in Aerator Test Procedures

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At any given temperature T:

$$W_T = (K_La)_T(C_T^* - C) \tag{1}$$

where T is expressed in °C, W_T is the oxygen mass transfer rate per unit volume in clean water in mg/(l sec), K_La is the overall liquid phase volumetric oxygen mass transfer coefficient in sec^{-1}, C_T^* is the saturation dissolved oxygen concentration for the water in mg/l and C is the dissolved oxygen concentration in the water in mg/l. If the water temperature increases from 10 to 20°C, the value of K_La can increase by more than 50% in some cases, while the value of C^* will decrease by approximately 20%. Thus, the influence of water temperature on aerator testing is quite significant and must be taken into account.

Saturation Dissolved Oxygen Concentration Tables

Use of C Tables*

The value of $(K_La)_T$ is not measured directly during an aerator test but is calculated. Some procedures for calculating $(K_La)_T$ require that C_T^* be known. The value of C_T^* is itself sometimes calculated. This is done by referring to a table that lists saturation dissolved oxygen concentrations in clean water at a pressure of one atmosphere for a wide range of water temperatures. The C_T^* value is calculated by using Henry's Law to adjust the saturation dissolved oxygen concentration in the table corresponding to the water temperature employed in the aerator test. The actual calculation is beyond the scope of this paper and will not be discussed here.

A partial sensitivity analysis was performed by Hunter and Ward (11) to determine how critical the value of C_T^* is in determining $(K_La)_T$. It was found that a deviation of ±0.10 mg/l from the correct value of C_T^* can cause an error of approximately 5% in the calculation of $(K_La)_T$. This takes on real significance when one considers that values of saturation dissolved oxygen concentration that appear in various published tables differ from one another by as much as 0.4 mg/l for clean water at 20°C and a pressure of one atmosphere.

Another important use of saturation dissolved oxygen tables is in the determinination of a value for the saturation dissolved oxygen concentration that can be used to calibrate the instrumentation for measurement of dissolved oxygen concentrations during aerator testing.

From all of the above, it can be seen that saturation dissolved oxygen tables are a necessity in performing an aerator test if the effects of water temperature are to be properly accounted for. As there are differences in the values of saturation dissolved oxygen concentrations between various tables in the literature and because, as shown by Hunter and Ward (11), these differences can cause appreciable variations in the value of $(K_La)_T$ calculated for any aerator test, it is imperative that an accurate table be used. This table should become a part of any standard to be adopted for aerator testing so that comparable results can be obtained from tests performed by different individuals.

Selection of C Table*

For many years, the most often used values for saturation dissolved oxygen concentrations were those of Fox (9), Winkler (20) (21) and Montgomery, Thom and Cockburn (14). Fox's values for a dry atmosphere were corrected by Whipple (19) to a water vapor saturated atmosphere. These are the values that have been included in the past several editions of Standard Methods for the Examination of Water and Wastewater. As noted in a review by Hunter (10), evidence has gradually accumulated over the years that shows these saturation dissolved oxygen concentrations are not accurate. The upcoming 15th edition of Standard Methods will contain a new table of saturation dissolved oxygen concentrations. This table is reproduced here as Table 1. The background behind this table is presented by Postma, Svansson, Lacombe and Grasshoff (16). This table is based on the 1973 International Oceanographic Tables jointly published by the National Institute of Oceanography and the United Nations Educational, Scientific and Cultural Organization under the supervision of SCOR-UNESCO-ICES-IAPSO Joint Panel of Experts for Oceanographic Tables and Standards. Table 1 represents the most up-to-date and accurate saturation dissolved oxygen concentration data currently available. It is recommended that Table 1 become a part of any standard to be adopted for aerator testing.

K_La Water Temperature Correction Factor

Present Practice

An aerator performance test determines a value for K_La at the water temperature employed during the test. In order to standardize aerator test ratings, it is usual practice to express the aerator test results in terms of

Table 1. Solubility of Oxygen in Water Exposed to Water Saturated Air (mg/l)

Temperature (°C)	Chloride Concentration in Water (mg/l) 0	5,000	10,000	15,000	20,000
0	14.60	13.72	12.90	12.13	11.41
1	14.19	13.35	12.56	11.81	11.11
2	13.81	12.99	12.23	11.51	10.83
3	13.44	12.65	11.91	11.22	10.56
4	13.09	12.33	11.61	10.94	10.30
5	12.75	12.02	11.32	10.67	10.05
6	12.43	11.72	11.05	10.41	9.82
7	12.12	11.43	10.78	10.17	9.59
8	11.83	11.16	10.53	9.93	9.37
9	11.55	10.90	10.29	9.71	9.16
10	11.27	10.65	10.05	9.49	8.96
11	11.01	10.40	9.83	9.28	8.77
12	10.76	10.17	9.61	9.08	8.58
13	10.52	9.95	9.41	8.89	8.41
14	10.29	9.73	9.21	8.71	8.24
15	10.07	9.53	9.01	8.53	8.07
16	9.85	9.33	8.83	8.36	7.91
17	9.65	9.14	8.65	8.19	7.76
18	9.45	8.95	8.48	8.03	7.61
19	9.26	8.77	8.32	7.88	7.47
20	9.07	8.60	8.16	7.73	7.33
21	8.90	8.44	8.00	7.59	7.20
22	8.72	8.28	7.85	7.45	7.07
23	8.56	8.12	7.71	7.32	6.95
24	8.40	7.97	7.57	7.19	6.83
25	8.24	7.83	7.44	7.06	6.71
26	8.09	7.69	7.31	6.94	6.60
27	7.95	7.55	7.18	6.83	6.49
28	7.81	7.42	7.06	6.71	6.38
29	7.67	7.30	6.94	6.60	6.28
30	7.54	7.17	6.83	6.49	6.18
31	7.41	7.05	6.71	6.39	6.08
32	7.28	6.94	6.61	6.29	5.99
33	7.16	6.82	6.50	6.19	5.90
34	7.05	6.71	6.40	6.10	5.81
35	6.93	6.61	6.30	6.01	5.72
36	6.82	6.51	6.20	5.92	5.64
37	6.71	6.40	6.11	5.83	5.56
38	6.61	6.31	6.02	5.74	5.48
39	6.51	6.21	5.93	5.66	5.40
40	6.41	6.12	5.84	5.58	5.33
41	6.31	6.03	5.76	5.50	5.25
42	6.22	5.94	5.68	5.42	5.18
43	6.13	5.85	5.60	5.35	5.11
44	6.04	5.77	5.52	5.27	5.04
45	5.95	5.69	5.44	5.20	4.98
46	5.86	5.61	5.37	5.13	4.91
47	5.78	5.53	5.29	5.06	4.85
48	5.70	5.45	5.22	5.00	4.78
49	5.62	5.38	5.15	4.93	4.72
50	5.54	5.31	5.08	4.87	4.66

$(K_La)_{20}$. This is done by dividing $(K_La)_T$ by the dimensionless water temperature correction factor F which is defined as follows:

(2) $$F = (K_La)_T/(K_La)_{20}$$

Two expressions are normally used for calculating F. The most common expression in the literature for F is:

(3) $$F = \theta_e^{(T-20)}$$

where θ_e is the dimensionless geometric temperature coefficient. The other expression for F is:

(4) $$F = c + \theta_l T$$

where c is a dimensionless constant which is the 0°C intercept of the plot F vs T, and θ_l is the slope of the plot F vs T, known as the linear temperature correction coefficient that is expressed as $°C^{-1}$. Reported values for θ_e range from 1.012 to 1.047 [Lakin and Salzman (12)] and reported values for θ_l range from 0.015 to 0.022 [Hunter and Ward (11)]. All reported values of θ_e and θ_l have been determined empirically. These apparent discrepancies in reported values of θ_e and θ_l are significant. For example, the temperature correction factor F for converting $(K_La)_{10}$ to $(K_La)_{20}$ calculated by using a value of 1.047 for θ_e is approximately 30% larger than F calculated by using a value of 1.012 for θ_e. This points out the need for establishing an accurate method for adjusting $(K_La)_T$ to $(K_La)_{20}$. This method should become a part of any standard to be adopted for aerator testing so that comparable results can be obtained from tests performed by different individuals.

Proposed Method for Determining $(K_La)_{20}$

Equations 3 and 4 indicate that the K_La water temperature correction factor F is exclusively dependent on water temperature. This is definitely not the case. The water temperature correction factor is also dependent upon turbulence. This is evident in empirical results reported by Elmore and West (8), Metzger (13), and Bewtra, Nicholas and Polkowski (1). In addition, Metzger (13), Rathbun and Bennett (17) and Camp and Meserve (4), starting with the film-penetration theory of gas-liquid mass transfer, have demonstrated that theoretically the K_La water temperature correction factor should be both a function of water temperature and turbulence and that the correction factor should increase with decreasing turbulence.

By combining turbulent flow theory with the film-penetration theory of gas-liquid mass transfer, it can be shown that it should be possible to express K_La for any aeration system as a function of G, the temporal mean velocity gradient, and other system variables [Hunter (10)], that is:

(5) $$K_La = f\ (G, \text{other system variables})$$

where, in terms of measurable parameters:

$$G = \sqrt{\frac{P/V}{\mu}} \qquad (6)$$

in which G is expressed in sec^{-1}, P is total power input into the water being aerated in ergs/sec, V is the volume of water being aerated in cm^3 and μ is the absolute viscosity of the water in poise.[1]

It can be seen from Equation 6 that G is temperature dependent because of the inclusion of absolute viscosity which is temperature dependent. The G term is also considered to be an index of turbulence. Thus, Equation 5 would inherently take both water temperature and turbulence into account without recourse to the use of a correction factor or factors.

It is important to note that the temperature correction factor F should not be calculated as the ratio μ_{20}/μ_T nor G_T/G_{20}, but as $(K_La)_T/(K_La)_{20}$ using K_La as described in Equation 5.

Hunter (10) has verified Equation 5 for the case of laboratory-scale submerged turbine aeration systems. For the tank geometry used, Hunter found that:

$$K_La = [4.04 + 0.00255G^2\ (D/T)^4]Q^{0.63} \qquad (7)$$

where K_La is expressed in hr^{-1}, G in sec^{-1}, D is impeller diameter in cm, T is tank diameter in cm and Q is air discharge rate in SCFH. By substituting P/V for G^2 in Equation 7, then substituting Equation 7 into Equation 2, one obtains:

$$F = \frac{[4.04 + 0.00255\ (D/T)^4\ (P/V\mu_T)]Q^{0.63}}{[4.04 + 0.00255(D/T)^4\ (P/V\ \mu_{20})]Q^{0.63}} \qquad (8)$$

For the case where D/T is 0.35, Q is 1.0 SCFH, P/V is 2000 ergs/sec·cm^3 and water temperature T is 10°C (μ_{10} = 0.01307 poise and μ_{20} = 0.01002 poise) [Weast (18)], F is 0.841. By decreasing P/V to 500 ergs/sec·cm^3, F becomes 0.924. Thus, Equation 8 is consistent with the theoretical work mentioned previously that showed F should be a function of both water temperature and turbulence and that F should increase with decreasing turbulence.

Data are presented by Hunter and ward (11) that show Equation 7 is valid over a water temperature range of $0 \leq T \leq 40°C$ ($32 \leq T \leq 104°F$) without the use of any correction factors. These data are presented in Table 2.

Thus, on both empirical and theoretical grounds, it appears that relationships for expressing K_La for aeration systems as a function of G and other pertinent system variables adequately take into account the effect of water temperature on K_La, including the influence of turbulence on this effect.

It is recognized that equations probably do not exist at the present time for full-scale aeration systems that express K_La as a function of G. Therefore, it is recommended research be undertaken to attempt to develop empirically such equations and to determine if such equations can adequately account for the effects of water temperature on K_La at different levels of turbulence.

In the meantime, specification of a single value for θ_e or c and θ_l in an aerator testing standard would be arbi-

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[1] Equation 5 is developed in the attachment. The reader is referred to Camp and Stein (3) and Camp (2) for derivation of Equation 6.

Table 2. Comparison of Measured K_La at Water Temperatures of $0 \leq T \leq 40°C$ with K_La Expressed as a Function of Temporal Mean Velocity Gradient G

Water Temperature (°C)	Absolute Viscosity* (poise)	K_La Based on Data from Hunter & Ward (11) (hr^{-1})	K_La Calculated from Relationship between K_La and G (hr^{-1})	ΔK_La (hr^{-1})	Percent Difference
0.0	0.01787	7.99	8.80	-0.81	-10
5.0	0.01519	9.12	9.64	-0.52	- 6
10.0	0.01307	10.26	10.55	-0.29	- 3
15.0	0.01139	11.39	11.51	-0.12	- 1
20.0	0.01002	12.53	12.53	0.00	0
25.0	0.008904	13.66	13.60	0.06	<1
30.0	0.007975	14.79	14.71	0.08	<1
35.0	0.007194	15.93	15.87	0.06	<1
40.0	0.006529	17.06	17.08	-0.02	<1

See pp. 91-92 of Hunter (10) for development of this table.

*Weast [(18), p. F-36]

trary and misleading. The effects of turbulence should result in different values for θ_e or c and θ_l for different aerators as evidenced by the wide ranges of values for θ_e and ϑ_l that have been reported in the literature. Aeration equipment manufacturers should be required to develop empirical values for θ_e or c and θ_l to be provided along with values of $(K_La)_{20}$ in order to describe more adequately the performance of the aerators they are marketing.

Conclusions

The effect of water temperature on oxygen mass transfer rate is significant. The physical mechanism involved is currently only speculative. In the absence of a comprehensible theory, empirical data available in the literature appear contradictory. A definite need exists for more research on this subject.

The following recommendations are put forth for consideration in the development of a standard aerator test procedure to ensure that the influence of water temperature on aerator testing is taken into account:

1. The revised saturation dissolved oxygen table that will appear in the upcoming 15th edition of Standard Methods and reproduced here as Table 1 should be incorporated into the aerator testing standard. The use of this table should be required for all calculations that require the use of saturation dissolved oxygen tables.
2. Research should be undertaken to attempt to empirically develop equations for full-scale aeration systems that would express K_La as a function of the temporal mean velocity gradient G and to determine if such equations can adequately account for the effects of water temperature on K_La at various levels of turbulence.
3. Aeration equipment manufacturers should be required to empirically develop values for θ_e or c and θ_l that would be provided along with values of $(K_La)_{20}$ in order to more adequately describe the performance of the aerators they are marketing.

It is understood that the above recommendations do not represent a final answer to the question of how to account for the effects of water temperature on aerator testing. It is hoped, however, that these recommendations will provide a basis for developing a test procedure that incorporates the best available information on this subject.

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Attachment

Relationship Between G and Oxygen Mass Transfer Rate

The physical process of oxygen mass transfer is the same whether it occurs in a bubble diffusion, submerged turbine, or a mechanical surface aeration system. That is to say, any one gas-liquid mass transfer theory (such as the two-film theory) can be used to model the oxygen mass transfer process that occurs in any type of aeration system. Therefore, if an accepted model of gas-liquid mass transfer indicates that K_L (the overall liquid phase oxygen mass transfer coefficient) can be expressed as a function of a measurable parameter such as temporal mean velocity gradient (G), it may be possible to express K_L or K_La for any aeration system as a function of that same parameter. The following discussion utilizes the film-penetration theory of gas-liquid mass transfer and turbulent flow theory to show that K_L or K_La can be expressed as a function of temporal mean velocity gradient (G).

The film-penetration theory was developed by Dobbins (6) using a physical model similar to that of the two-film theory. Dobbins envisioned the liquid film to exist in a statistical sense with the film always present and its contents being randomly exchanged with portions of the bulk liquid. The film-penetration theory states that:

(A-1) $K_L = (rD_m)^{1/2} \text{ ctnh } (rL_L^2/D_m)^{1/2}$

in which K_L is expressed in cm/sec, r is the fraction of gas-liquid interface being replaced for each unit of time in $\sec^{-1}$, D_m is the coefficient of molecular diffusion of the gas in the liquid expressed as cm^2/sec and L_L is the liquid film thickness in cm.

It can be seen that K_L in Equation A-1 is a function of two parameters, r and L_L. Both parameters are characteristic of the hydrodynamics of the system. When turbulence increases, r increases and L_L decreases. In his laboratory experiments, Dobbins (7) found that a consistent relationship exists between r and L_L that is independent of mixing conditions. Because r and L_L are not independent of each other, K_L can be considered to be expressed as a function of r in Equation A-1.

O'Connor and Dobbins (15) developed stream aeration concepts based on the film-penetration theory of gas-liquid mass transfer. They considered r, the rate of gas-liquid interfacial surface renewal, to be:

(A-2) $r = \sqrt{v^2}/l$

where $\sqrt{v^2}$ is the vertical root mean square velocity fluctuation of the liquid at the liquid surface expressed in cm/sec and l is the Prandtl mixing length in cm defined as:

(A-3) $l = K\,(dv/ds)/(d^2v/ds^2)$

where $0.3 \leq K$ (a constant) ≤ 0.45 and dv/ds is the velocity gradient expressed in $\sec^{-1}$.

According to the Prandtl mixing theory:

(A-4) $l\,(dv/ds) = \sqrt{v^2}$

By rearranging terms:

(A-5) $dv/ds = \sqrt{v^2}/l$

Substituting Equation A-5 into Equation A-2 gives:

(A-6) $r = dv/ds$

By definition, at a finite point [Camp and Stein, (3)]:

(A-7) $G = dv/ds$

By substituting Equation A-7 into Equation A-6, it is found that:

(A-8) $r = G$

It was stated previously that the film-penetration theory expressed overall liquid phase oxygen mass transfer coefficient K_L as a function of r. However, as shown above, r equals G so that it is also possible to express K_L as a function of G. Furthermore, because the film-penetration model is generally assumed to be applicable to the process of oxygen mass transfer in any aeration system, it may be possible to express K_L for all aeration systems as a function of G. The same is true for K_La because K_La is simply the product of K_L and a, which is the gas-liquid interfacial surface area per unit volume.

It is known that aeration system variables such as aeration tank geometry and (except for surface aerators) air flow rate have a significant effect on oxygen mass transfer rate and must be taken into account. Therefore, for any particular aeration system:

(A-9) $K_La = f(G, \text{other system variables})$.

Influence of pH and Iron and Manganese Concentrations on the Non-Steady State Clean Water Test for the Evaluation of Aeration Equipment

15

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Introduction

The non-steady state clean water test is the most widely accepted procedure for the evaluation and comparison of the oxygen transfer efficiency of various types of aeration equipment. First, a known quantity of clean tap water is chemically deoxygenated to a very low dissolved oxygen (DO) concentration by the addition of sodium sulfite as a reducing agent. Typically, about one and one-half times the sulfite required to stoichiometically react with the DO present is used (17). High sulfite addition is reported to result in high $(K_La)_{20}$ values (6)(13).

Since the rate of the sulfite auto-oxidation reaction is very slow and the presence of sulfite ions interferes with Winkler DO determinations, addition of a catalyst is necessary to increase the rate of sulfite oxidation (12). The accepted catalyst is colbalt (II) chloride hexahydrate or other cobalt (III) salts (Co^{++}). Conventionally, 2-5 mg/l or even as high as 10 mg/l of Co^{++} has been recommended and employed (9). Chemical deoxygenation is followed by oxygenation and data collection. During the oxygenation (aeration) process the rate of oxygen transfer from the gas phase into the liquid phase is determined by measuring the dissolved oxygen concentration as a function of time.

The presently accepted non-steady state clean water test to evaluate aerator performance is reported to have caused interference in the Winkler DO determinations (9)(11)(13)(14). A precipitate is reported to cause the interference. This interfering precipitate is postulated to be an unidentified cobalt complex or cobalt (III) hydroxide (9)(10).

Accordingly, various recommendations have been made to eliminate this interference. An initial cobalt (II) ion concentration of 0.05 mg/l is recommended by Kalinske et al. (9), while the usage of 2 mg/l of cobalt combined with a determination of the degree of interference for each aeration test has been suggested by Lakin (10). No theoretical justification, however, is submitted nor discussed by either Kalinske or Lakin to support their recommendations.

In contrast to Lakin's and Kalinske's observations, insignificant variation in oxygen transfer efficiency was found by using up to 5 mg/l of cobalt in aeration tests using city tap water at Melbourne, Australia and Rochester, New York (1)(11).

These latter results are in agreement with the results of the non-steady state clean water tests obtained by Eimco Process Machinery Division (PMD) in the Northeastern United States. No differences in oxygen transfer efficiency were found when adding as much as 8 mg/l cobalt and then using Winkler or polarographic methods to determine DO concentration. On the other hand, under identical conditions and with the same type of aerator, significant interference with the Winkler DO determination was observed using Salt Lake City tap water.

The inconsistent results reported in the literature and experienced by Eimco PMD prompted the initiation and continuation of diagnostic studies at Eimco. Phases I and II of this work were designed to define the following aspects:

1. To identify and determine the causes and mechanisms of so-called cobalt interference
2. To develope a predictive model for the so-called cobalt interference
3. To explain the inconsistency of cobalt interference for different natural water systems.

The results of these studies are reported elsewhere (13)(14).

The purpose of the present study was to quantify further the influence of water chemistry based on recent findings and to identify an interference-free, non-steady state clean water method to compare and objectively determine transfer efficiencies of various types of aeration equipment using different natural waters in the test system.

Theoretical Considerations and Summary of Earlier Findings

The chemistry-interfering aspects of the Winkler DO, the electrochemistry of cobalt ion species in aqueous solution, sulfite oxidation products and formation of hydrogen peroxide during aeration testing are reported in detail elsewhere (13)(14). The essential findings contained in the referenced work are reported here to provide clarity and continuity to the present paper.

It was found that the interference in the non-steady state test was not caused by the precipitate alone, as is postulated by others (9)(10). Actually, up to 80-90% of the error for DO measurements by the Winkler method can be attributed to one or more soluble components if the initial cobalt concentration is less than 2 mg/l. One soluble component was determined to be hydrogen peroxide (H_2O_2) which is a decomposition product of peroxy monosulfuric acid (H_2SO_5). The formation of peroxy monosulfuric acid in the presence of oxygen, sulfite and a catalyst in aqueous solution was known and reported as early as 1934 by Baeckstroem (2).

At relatively high initial cobaltous ion concentrations, most of the hydrogen peroxide is catalytically decomposed due to the formation of suspended solids at natural tap water pH levels. The catalytic decomposition of hydrogen peroxide in the presence of surface active substances such as metal oxides and other colloidal matter is well known (7).

Electron spin resonance (ESR) spectroscopic analysis and oxygen equivalent measurement, together with other chemical identification and structural analysis methods, have shown that the interfering precipitate consists mainly of hydrated trivalent and tetravalent cobalt oxide. This interfering precipitate was determined to be formed at pH values of 6.9 and above. No precipitate was formed, however, if the pH was kept below 6.9.

Furthermore, the pH and the buffering capacity of test water were determined to be the most critical water quality parameters which directly affected the magnitude of the interference phenomenon. Accordingly, it was recognized that a precise quantitative prediction could not be made of the level of cobalt interference for various natural water systems. This also was observed if the wastewater was physically deoxygenated (nitrogen stripped). Different water systems under uniform and constant experimental conditions (constant power intensity, constant rpm, constant air flow) showed different $(K_La)_{20}$ values when using the same prototype laboratory submerged turbine aerator. The data from our previous work are summarized in Table 1 (14).

In general, the so-called cobalt interference is soft water systems was determined to be minimal or totally absent. Water systems with higher pH and substantial alkalinity caused interference of unpredictable magnitude. Re-evaluation of the work previously reported indicated that there may be a correlation between the alkalinity of the test water and the magnitude of the interference. Dallas, Texas water, which has a relatively low alkalinity, showed an unexpectedly high $(K_La)_{20}$ value. On the other hand, the Dallas, Texas test water collected from the aeration test basin showed an unusually high iron concentration of 25 mg/l. It was suspected that the high iron concentration could provide some sort of enhancement of oxygen transfer. This aspect, however, was not discussed or quantified in our previous study. This aspect will be quantified in this present report.

Another aspect of the catalyzed sulfite oxidation reaction, which was not previously discussed, is the pH dependence and the catalytic mechanism of cobalt. For example, does the cobalt act as a homogeneous or heterogeneous catalyst? If cobalt acts as a heterogeneous catalyst, then the rate of sulfite oxidation at low pH should be very slow and probably some sulfite will remain in the solution which will interfere with Winkler DO measurements. These and other questions are dealt with as a part of the present paper.

The rate of the non-catalytic oxidation of sulfite with DO, as a function of sulfite ion concentration for pH values of

Table 1. Previously Reported Oxygen Transfer Data (14)

						Method of Deoxygenation				
	Water Quality Parameter					N_2-Strip			One $SO_3^=$ Addition	
Geographical Location	Initial pH	Alkalinity (mg/l) as $CaCO_3$	Test Water Temp. (°C)	DO Saturation C* (mg/l)	Iron (mg/l)	Probe $(K_La)_{20}$ (hr^{-1})	Winkler $(K_La)_{20}$ (hr^{-1})	pH after N_2-Strip	Probe $(K_La)_{20}$ (hr^{-1})	Winkler $(K_La)_{20}$ (hr^{-1})
Harrisburg, Virginia	7.4	192.0	22.0	7.51	0.55	8.32	9.44	8.1	8.49	12.84
Spokane, Washington	7.0	152.0	22.5	4.48	—	8.27	8.15	7.6	8.57	10.17
Rochester, New York	7.1	80.0	23.4	7.4	0.55	5.77	6.32	7.9	5.66	7.16
Springfield, Mass.	7.5	50.0	22.0	7.48	0.75	4.09	4.07	8.0	4.40	
Dallas, Texas	7.5	41.0	21.0	7.5	25.0	7.3	7.22	7.2	8.3	7.76
Concord, New Hampshire	6.7	24.0	22.5	7.9	0.9	4.45	7.1	7.1	4.36	4.45

6.5-7.7 for a DO concentration greater than 0.8 mg/l and at 20°C, was studied and defined by Gale (4). Under these conditions the rate is:

$$dC/dt = 1.46 \times 10^{21} (SO_3^{=})^{2.33} (H^{+})^{1.58}$$

Furthermore, it was found that the rate of sulfite oxidation by DO, over the pH range of 4.2-6.4 and under the above mentioned conditions, was dependent upon the first power of sulfite ion concentration and some unidentified variable and was very slow (4).

As early as 1897, it was observed that mixtures of compounds susceptable to auto-oxidation often gave results quite different from those formed when a single compound alone was studied. In particular, certain compounds which reacted very slowly with oxygen in pure solutions were oxidized much more rapidly when sulfite was presented (8). Also, the rate of sulfite auto-oxidation was found to be markedly affected by various materials (3)(5)(8)(18).

The studies cited above attempted to explain these results in terms of various hypothetical reaction mechanisms for relatively high sulfite concentrations in aqueous solutions. The proposed mechanisms often conflicted in detail, many were rather vague and all were basically speculative in nature. Nonetheless, most studies contained the postulate that some kind of inactivated intermediate molecule, ion, and/or radical was necessary to explain the observed results (4). Specifically, the activation of oxygen in the presence of $SO_3^{=}$ ion and some heavy metals were the interpretation of the above observed phenomenon. Baeckstroem proposed the chain reaction mechanism which is now most generally accepted as the true explanation for the catalytic activation of oxygen molecules by certain metals (2). A chain reaction is caused by the presence of radicals. Such mechanisms of oxygen activation in the presence of transitional elements in either the heterogeneous or homogeneous phase, with and without sulfite addition, can also be assumed and postulated in reaeration testing using physical or chemical deoxygenation processes followed by oxygenation.

A hypothesis which envisions the activation of oxygen by metals and/or metal oxides is as follows. Dissolved oxygen in the presence of certain metals is activated (O_2^{*}). This oxygen molecule can react with water to form $OH^{\cdot}$ radicals or hydrogen peroxide and atomic oxygen (or oxygen radicals) as follows:

$$O_2^{*} + H_2O \rightleftarrows O^{\cdot} + 2OH^{\cdot}$$

Reaction of these products will result in the formation of oxygen molecules and water as follows:

$$OH^{\cdot} + OH^{\cdot} \rightleftarrows H_2O_2 \longrightarrow H_2 1/2\ O_2$$

$$O^{\cdot} + O^{\cdot} \longrightarrow 1/2\ O_2$$

This reaction path is established and is a well known mechanism in the fields of photochemical and ozone chemistry. In the practice of aeration testing it is often observed that during the oxygenation step the vicinity of the aeration basin smells chlorine- or ozone-like. The production of chlorine in certain natural waters with high Cl^{-} concentration can only be explained by the presence of radicals, especially the hydroxyl radical which has a redox potential of 2.8 volts (15).

Experimental Procedures

A 4,226 liter plexiglass tank approximately 5.9 square meters in surface area and 1.2 meters in height was used to conduct the non-steady state reaeration tests. Reareation was accomplished by using a geometrically and kinematically scaled-down version of the Eimco PMD submerged turbine aerator. The turbine was 21.8 centimeters in diameter and was submerged to a depth of 0.5 meter below the water surface. The air flow rate, 561 l/min, was metered and controlled by an accurate flow meter (±2% full-scale) manufactured by Fisher-Porter. Air to the turbine was supplied via an air compressor.

To achieve uniform mixing intensity, the mixer for all experiments was operated at 400 rpm. The rotational speed was determined with a Pioneer Photo Tach Model 36 (±2% full scale). The temperature and pressure of the air flow to the submerged turbine were monitored at the point of exit from the flow meter.

Dissolved oxygen (DO) was measured using the Azide modification of the Winkler method (16).

To determine the magnitude of the interference occurring with the Winkler procedure, DO was also measured and recorded continuously during each run by the polarographic method using a YSI Model 54 DO meter and a Soltech Model 100 recorder. A probe with an integral mixer near the probe membrane was used to record DO polarographically. The probe was calibrated daily using Winkler DO determinations. The pH of the test water was monitored by a Sargent-Welch meter.

A 0.0015 molar phosphate buffer was used to adjust and maintain pH within 6-8. Salt Lake City tap water was used in this work.

The average makeup of test water was as follows:

Alkalinity - $CaCO_3$:180 mg/l
pH - 8.2
Fe - less than 0.01 mg/l
Mn - less than 0.1 mg/l

To determine the effect of pH on mass transfer, the test water was acidified to the desired pH and buffered with 0.0015 molar phosphate by adding proper mass ratios of K_2HPO_4 and KH_2PO_4 to keep the pH constant during consecutive reareation testing.

The addition of sulfite required for one test did not change the pH and no buffering of the test water was necessary. Consecutive reaeration tests were run for various pH values across the pH range of 4 to 9. Consecutive reaeration tests consisted in this case of physical deoxygenation-reoxygenation (N_2 strip-reaeration) and one chemical deoxygenation by means of excess sulfite addition to the test water, followed by reaeration and DO data collection. About 100 mg/l Na_2SO_3 as $SO_3^=$, technical grade, was used in all experiments.

It was noticed that at the low pH, one sulfite addition did not decrease the DO level sufficiently and a second sulfite addition was necessary. Sulfite and cobalt were added first and then after deoxygenation of the test water the pH was adjusted. For each consecutive reaeration test, fresh tap water was used. All tests were run at a cobalt ion concentration of 2 mg/l.

To determine the effect of iron and manganese on transfer efficiency, 2 mg/l each of iron and manganese were added to the test system.

All parameters were determined in accordance with the procedures specified in Standard Methods (16). The metal concentration was determined with an atomic absorption spectrophotometer Model 120 made by Varion Techtron. A schematic diagram of the experimental setup is shown in Figure 1.

Results and Discussion

The Effect of Hydrogen Ion Concentration on Sulfite Oxidation by DO

Figure 2 shows the kinetics of sulfite oxidation with DO during a deoxygenation run at pH 6. In order to keep the pH constant as described earlier, a mixture of KH_2PO_4 and K_2HPO_4 was added to the test water to buffer the system. The molarity of the test water was brought to 0.0015 molar phosphate.

At pH 6, one sulfite addition, 100 mg/l as $SO_3^=$ and 2 mg/l cobalt brought the DO to about 4.2 mg/l from the initial DO concentration of 8.8 mg/l within 1 min reaction time. After 1 min, the path of sulfite oxidation was changed dramatically and the rate decreased substantially. Within the next 5 min, the oxygen level was decreased from 4.2 to only 4.15 mg/l. Accordingly, 100 mg/l of additional sulfite was necessary to bring the DO to a desired level. As shown in Figure 2, the kinetics of the second sulfite addition during the same run seem to be very similar to that of the first. Reoxygenation of this test water as measured by the probe is shown on Figure 3, which is a typical oxygenation curve. Polarographic DO measurement is not affected by the presence of $SO_3^=$ ion as we previously reported (4).

DO was also measured by the Winkler method ten times during the test run. The Winkler DO measurement, however, indicated zero dissolved oxygen concentration in all ten samples indicating the presence of unreacted $SO_3^=$ ions. In the first step of the Winkler procedure, formation of MnO_2 was evident for all ten samples which indicates the presence of DO in the sample after reaeration. However, no iodine was formed during the next step of the Winkler DO measurement by the addition of potassium iodide followed by the acidification of the sample. This indicates the presence of $SO_3^=$ which was acting as a reducing agent and competing with the I^- ions The polarographic DO measurements were not affected by the presence of $SO_3^=$ ions as was previously mentioned (4). This is in agreement with the above result.

Figure 4 shows the result of chemical deoxygenation by addition of 100 mg/l $SO_3^=$ at pH 7 and 2 mg/l cobalt as a catalyst. At pH 7 the rate of catalytic sulfite oxidation was very fast as expected.

The preceding indicates that a water system with relatively low alkalinity needs more sulfite addition than the recommended amount of one and one-half times the stoichiometrical requirement for the reduction of DO initially present (about 100 mg/l Na_2SO_3 as $SO_3^=$).

Furthermore, the above results indicate that:

1. Catalytic sulfite oxidation with DO is pH dependent and the rate may be increased substantially by increasing the pH from 6 to 7.
2. Catalytic oxidation of sulfite with DO in the presence of cobalt probably can be explained by heterogeneous mechanism. Hydrated cobalt (II) and (III) oxide probably act as surface active catalysts, while at a pH value of 6.5, the cobalt (III) ion is in the form of a homogeneous catalyst.

The results of this study are in agreement with the results of several other studies of catalytic and non-catalytic sulfite oxidations in aqueous systems (2)(3)(5)(8)(18).

To avoid unnecessary high sulfite additions and in order to make correct DO measurements, the test water was deoxygenated (Salt Lake City tap water has an average pH of 8.2 to 8.3) and the pH then adjusted to the desired value by addition of phosphate. This technique was used in the experiments to determine the relationship between pH and $(K_La)_{20}$.

The Effect of Iron and Manganese on the Overall Mass Transfer Coefficient

Several experiments were conducted to determine the effects of iron and magnanese on the overall mass transfer coefficient. These two transition elements are natural tap water constituents, and the concentrations of both differ in tap water samples from different sources.

The results of these studies using both methods of deoxygenation and measuring the DO with both conventional methods are shown in Tables 2 and 3.

Figure 1. Equipment Arrangement

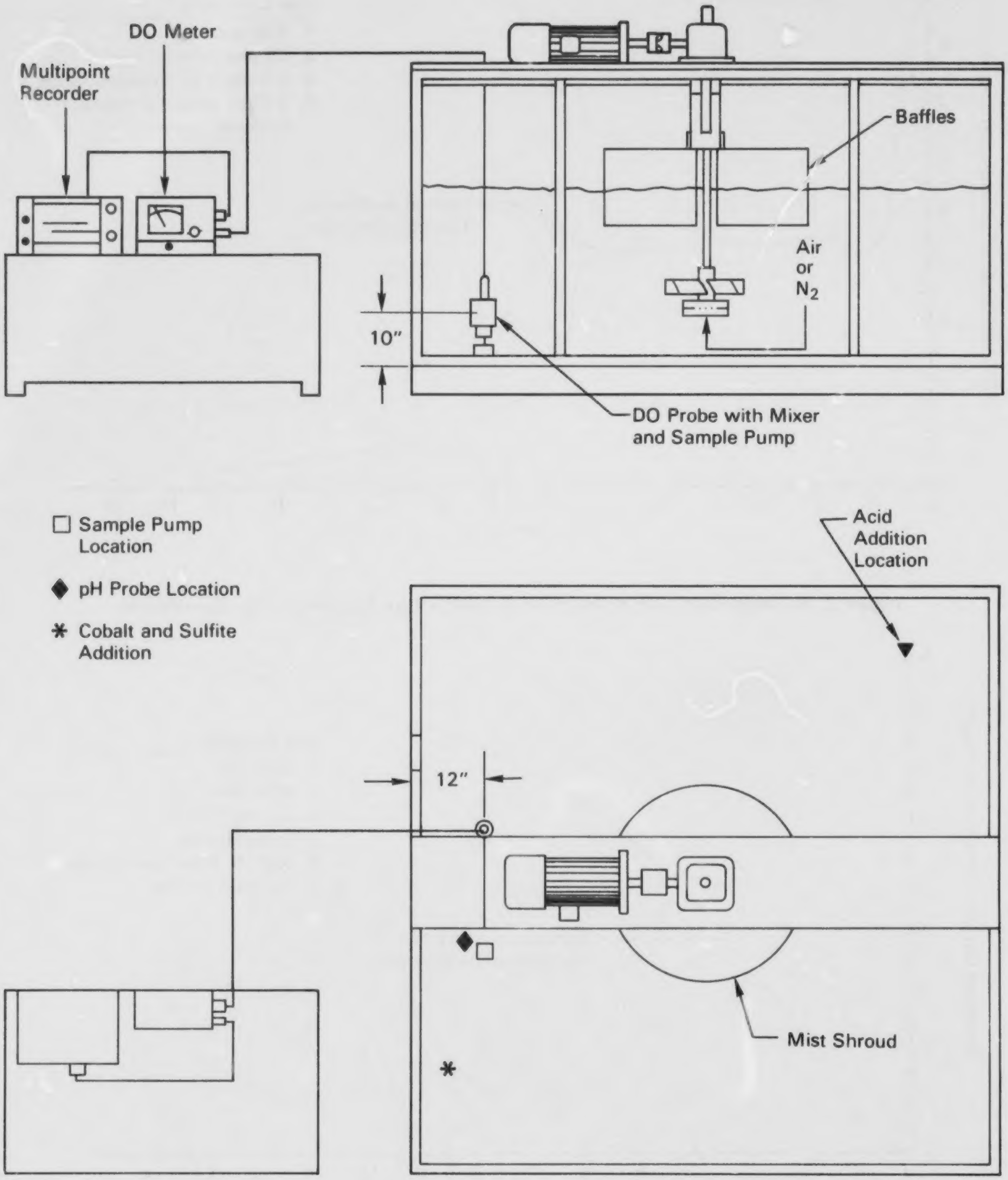
DO Meter
Multipoint Recorder
Baffles
Air or N_2
10"
DO Probe with Mixer and Sample Pump
□ Sample Pump Location
◆ pH Probe Location
* Cobalt and Sulfite Addition
Acid Addition Location
12"
Mist Shroud

Figure 2. Chemical Deoxygenation at pH Level of 6.0

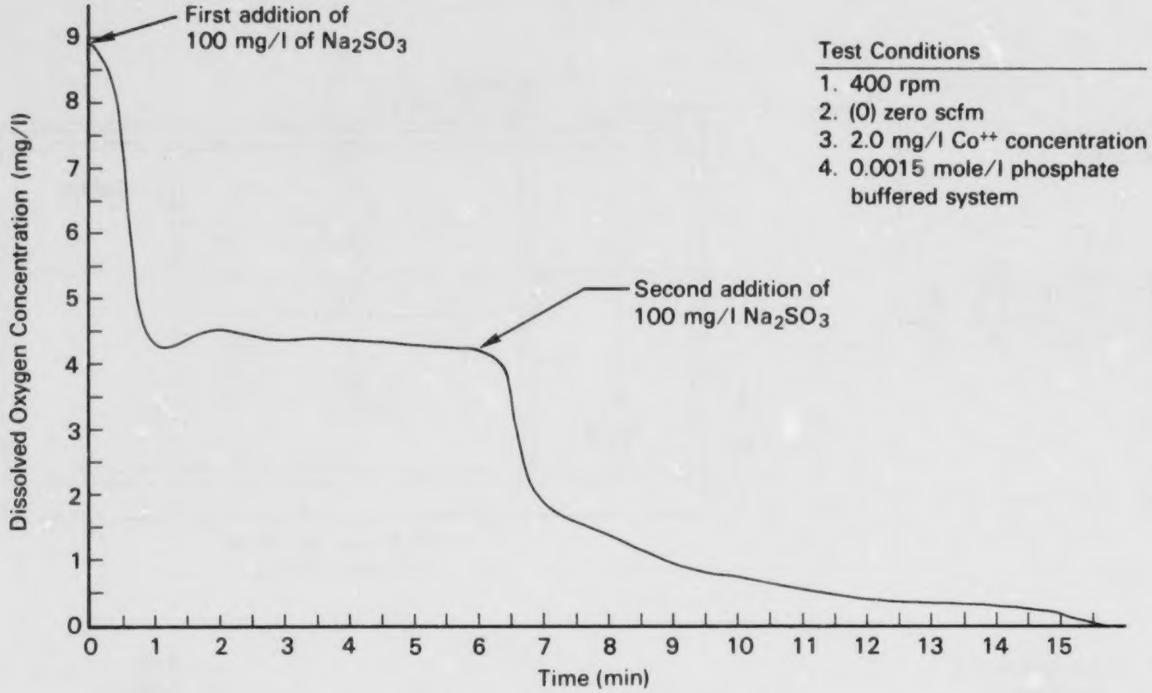

Figure 3. Reaeration of Test Water at pH 6.0 After Two Additions of Sodium Sulfite

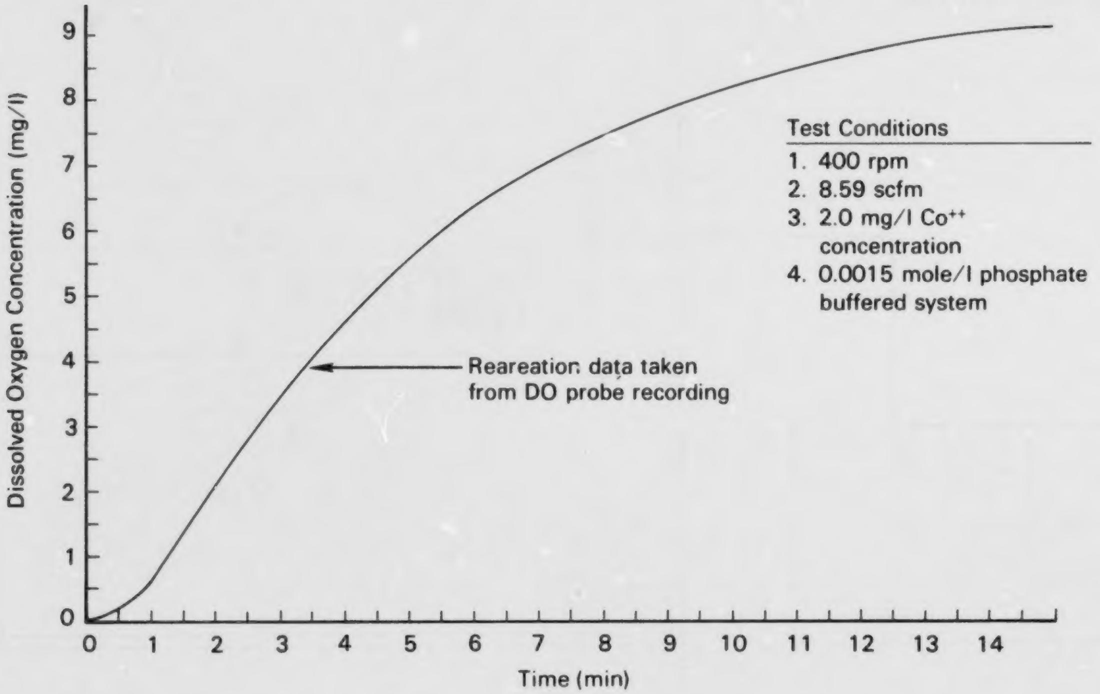

Figure 4. Consecutive Deoxygenation and Reaeration at pH 7.0 in a Phosphate Buffered System

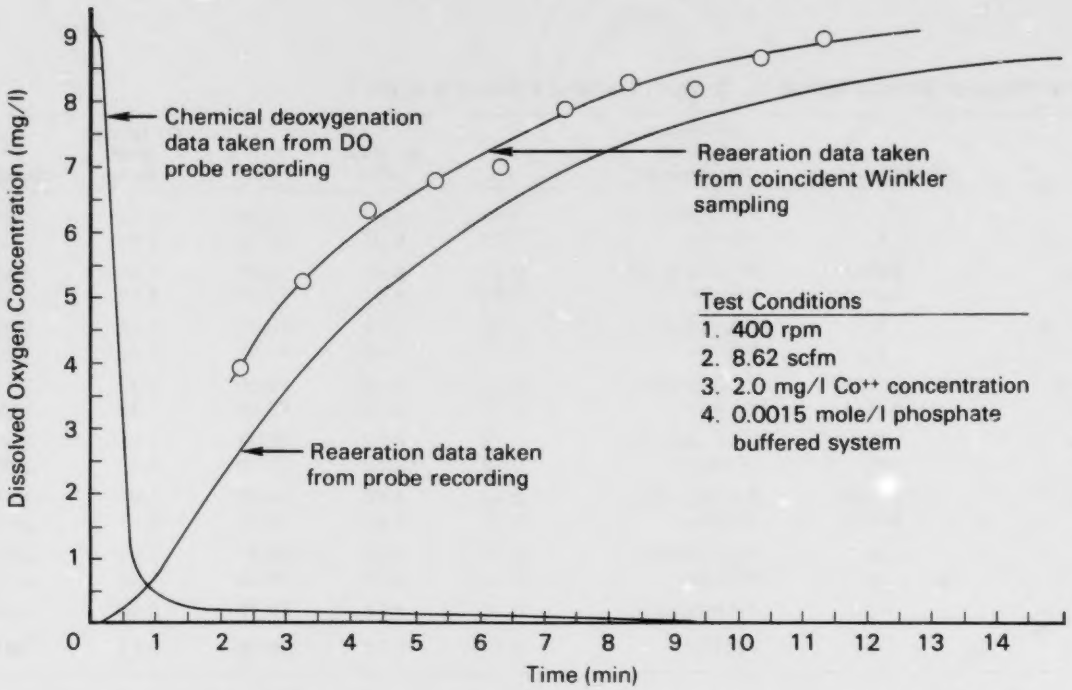

Figure 5. The Effect of pH and Buffering Capacity of Test Water on Overall Mass Transfer Coefficient Using Two Conventional Deoxygenation Processes; DO Measurements Taken with Winkler Titrations

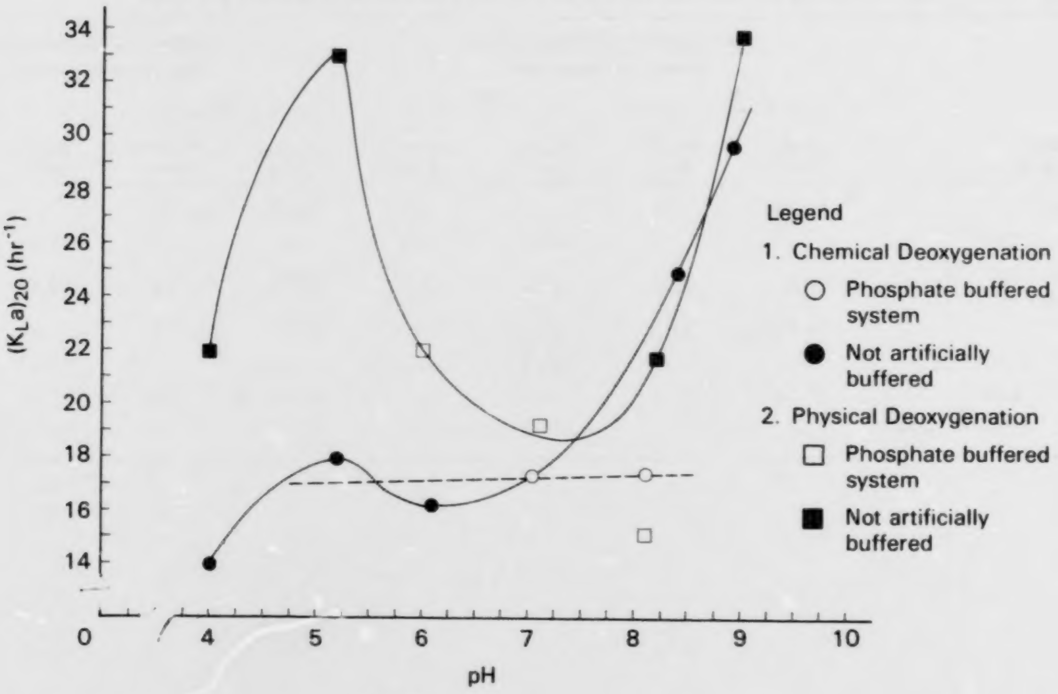

Table 2. The Effect of Various Metals on Oxygen Transfer Efficiency at pH 7

Additive (Catalyst)	Method of Deoxygenation	Method of Oxygen Determination	hp	Air Flow (scfm)	$(K_La)_{20}$ (hr^{-1})	O_2 Transfer Rate (lb/hr)	OTE (lb/hp·hr)
None	N_2	Polarographic	0.76	8.67	14.26	1.19	1.56
	N_2	Winkler	0.76	8.67	19.16	1.57	2.06
2 mg/l Co^{++}	Sulfite	Polarographic	0.73	8.61	13.05	1.09	1.50
	Sulfite	Winkler	0.73	8.61	17.27	1.42	1.95
2 mg/l Co^{++} &	N_2	Polarographic	0.73	8.53	20.40	1.66	2.26
2 mg/l Fe^{++}	N_2	Winkler	0.73	8.53	16.57	1.45	1.97
2 mg/Co^{++} &	Sulfite	Polarographic	0.73	8.53	19.80	1.61	2.13
2 mg/l Fe^{++}	Sulfite	Winkler	0.73	8.53	17.59	1.45	1.97
2 mg/l Mn^{++}	N_2	Polarographic	0.72	8.53	18.93	1.55	2.15
	N_2	Winkler	0.72	8.53	22.32	1.80	2.49
2 mg/l Co^{++} &	Sulfite	Polarographic	0.72	8.55	18.93	1.55	2.93
2 mg/l Mn^{++}	Sulfite	Winkler	0.72	8.55	26.57	2.11	2.15
2 mg/l Mn^{++} &	N_2	Polarographic	0.72	8.56	25.37	2.02	2.81
2 mg/l Fe^{++}	N_2	Winkler	0.72	8.56	18.59	1.52	2.11
2 mg/l Co^{++} & 2 mg/l Mn^{++} &	SO_3	Polarographic	0.72	8.55	33.72	2.60	3.70
2 mg/l Fe^{++}	SO_3	Winkler	0.72	8.55	34.68	2.66	3.61

Table 3. Increase in $(K_La)_{20}$ by Addition of 2 mg/l Initial Catalyst at Constant pH of 7

	Polarographic DO Measurement				Winkler DO Measurement			
	Method of Oxygenation				Method of Oxygenation			
	N_2		$SO_3^=$		N_2		$SO_3^=$	
Additive (Catalyst)	$(K_La)_{20}$ (hr^{-1})	% Relative Error	$(K_La)_{20}$ (hr^{-1})	% Relative Error	$(K_La)_{20}$ (hr^{-1})	% Relative Error	$(K_La)_{20}$ (hr^{-1})	% Relative Error
None	14.26	0.0	—	—	19.16	0.0	—	—
2 mg/l Co^{++}	—	—	13.05	0.0	—	—	17.27	0.0
2 mg/l Co^{++} & 2 mg/l Fe^{++}	20.4	43.0	19.80	51.7	16.57	-13.5	17.59	1.9
Mn^{++}	18.93	32.7	—	—	22.31	6.1	—	—
Co^{++} & Mn^{++}	—	—	18.93	45.0	—	—	26.57	52.0
Mn^{++} & Fe^{++}	25.37	77.9	—	—	18.59	-3.0	—	—
Co^{++} & Mn^{++} & Fe^{++}	—	—	33.72	158.3	—	—	34.68	100.8

The data obtained without adding any chemicals were used as the baseline for physical deoxygenation-reoxygenation data analysis while the data obtained with 2 mg/l cobalt were used as the baseline for chemical deoxygenation-reoxygenation (Table 3). The concentration of the metal salts was 2 mg/l as metal ion for each component. The iron concentration of 2 mg/l and manganese ion concentration of 2 mg/l are relatively high and do not represent the situation for most tap water supplies. However, these concentrations were used in our work to quantify the effects of these transition elements on the overall mass transfer coefficient.

The results of this study definitely demonstrate that the addition of iron and manganese affects the overall mass transfer and results in a range of $(K_La)_{20}$ values (Table 2 and 3). Measuring DO with a probe resulted in a relative error in $(K_La)_{20}$ as high as 158% using sulfite as the deoxygenation method and 78% using nitrogen stripping as the deoxygenation method. The relative error in $(K_La)_{20}$ for DO measurement with Winkler methods for nitrogen stripping was relatively small and ranged from −13.5% to +6.1%, while the error using sulfite deoxygenation ranged from 2 to 101% for various rates of sulfite addition.

It is known that various amounts of cobalt (III) ion addition in reaeration testing affects oxygen mass transfer (9)(13)(14). Accordingly, it can be postulated that various amounts of iron and manganese, and probably other transitional elements, even in very low concentrations will have different synergistic effects on oxygen mass transfer.

The $(K_La)_{20}$ data obtained by using nitrogen stripping and measuring DO with a probe suggest a mass transfer enhancement by addition of iron and manganese into the test water. Activation of DO in the presence of transitional elements is the most likely explanation of this enhancement phenomenon.

Method of DO Measurement and Deoxygenation in Relation to the Hydrogen Ion Concentration

Gaden (6) proposed the use of nitrogen gas instead of sodium sulfite for deoxygenation of test water. The use of sulfite as a reducing agent is widely accepted and practiced in the field of aerator performance evaluation. No quantitative analysis, however, is available to show the effect of altering the pH using physical or chemical deoxygenation methods and measuring the DO with a probe or the Winkler method.

Figure 5 shows the relationship between pH and the overall mass transfer for the two suggested deoxygenation processes while measuring DO by the Winkler method. At a pH value of 7 in a phosphate buffered system, there was little difference in the $(K_La)_{20}$ value between chemical and physical deoxygenation. However, the $(K_La)_{20}$ increased substantially for nitrogen deoxygenation-reoxygenation below and above pH 7. $(K_La)_{20}$ for pH 5 and 9 is about 94% higher than at pH 7. The $(K_La)_{20}$ decreases from 33 hr^{-1} at pH 5 to 22 hr^{-1} at pH 4 (Figure 5).

Data obtained by using sulfite deoxygenation-reaeration and measuring DO with the Winkler method indicates that the $(K_La)_{20}$ is independent of hydrogen ion concentration in a pH range of 5 to 7. By increasing the pH without phosphate addition, the value of $(K_La)_{20}$ increased; e.g., at a pH value of 8.4, the value of $(K_La)_{20}$ was 47% higher and at a pH of 9 this value was 70.5% higher than at a pH of 7 (phosphate buffered).

If the test water was buffered by a 0.0015 molar phosphate system to pH 8, the $(K_La)_{20}$ value remained constant at about 17 hr^{-1}.

Figure 6 shows the relationship of pH and overall mass transfer using a probe for DO determination and applying both methods of deoxygentation. The $(K_La)_{20}$ data for both deoxygenation processes were similar. The $(K_La)_{20}$ vs pH showed a constant value of 14 hr^{-1} for the pH range tested in a buffered system. The $(K_La)_{20}$ value for the non-phosphate buffered system was about 21% higher than the $(K_La)_{20}$ value for the phosphate buffered system. This result was in agreement with the data obtained at pH 7 in a buffered system when the system was chemically deoxygenated and when the DO was measured with the Winkler method. The data obtained during this series of experiments are summarized in Table 4.

Conclusions

An accurate quantitative prediction cannot be made of the magnitude of cobalt interference for various natural water systems with different water quality characteristics.

Chemical deoxygenation-reoxygenation in a phosphate buffered system while employing the Winkler DO measurement technique resulted in relatively precise $(K_La)_{20}$ values, especially at a pH of 6.9.

Consistent but different $(K_La)_{20}$ values were obtained in phosphate and non-phosphate buffered systems when the systems were deoxygenated by sulfite addition or nitrogen stripping within the pH range evaluated, when probes were used to measure DO, and when iron and manganese concentrations were at low background levels.

Iron and manganese and probably other transitional elements substantially enhance (up to 100% in this evaluation) oxygen mass transfer from the gas phase to the liquid phase during non-steady state clean water testing.

The data obtained from non-steady state clean water testing can be useful in comparison of aerator efficiencies

Figure 6. The Effect of pH and Buffering Capacity of Test Water on Overall Mass Transfer Coefficient Using Two Conventional Deoxygenation Processes; DO Measurements Taken with DO Probe

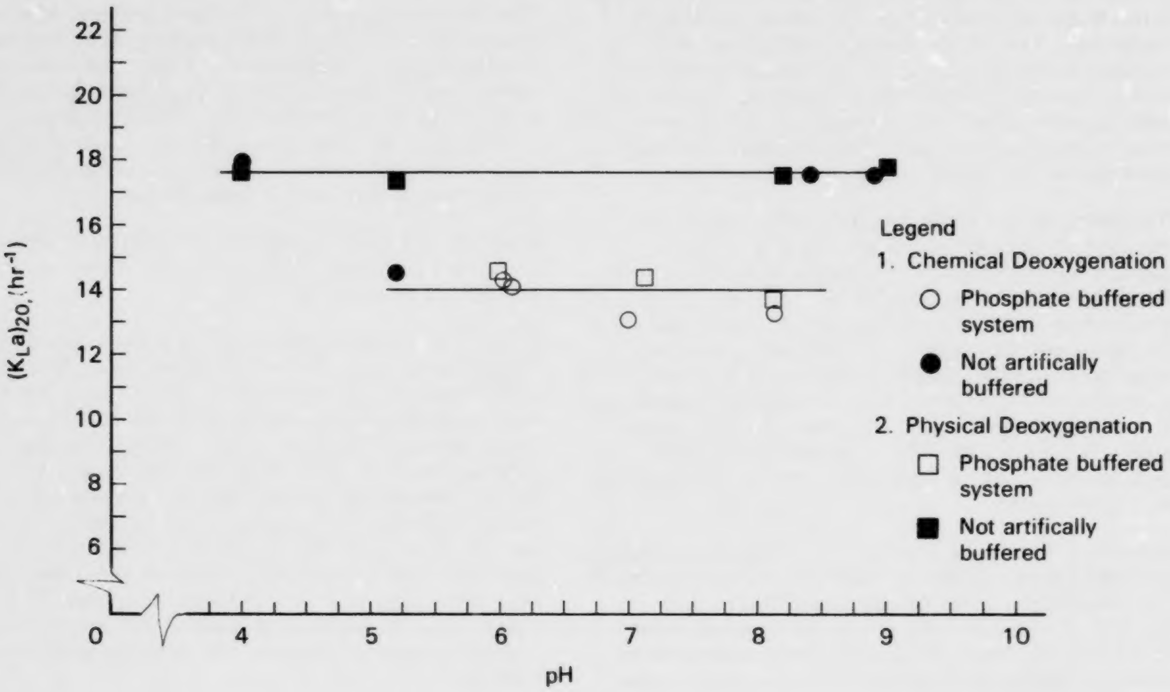

Table 4. Summary of Consecutive Reaeration Testing at Various pH Values Using 2 mg/l Initial Co^{++}

Run #	Method of Deoxygenation	Method of Oxygen Determination	pH Before Run	pH After Run	hp	scfm	$(K_La)_{20}$ (hr^{-1})	O_2 Transfer Rate (lb/hr)	OTE (lb/hp·hr)	Test Water Fe Conc. (mg/l)
1	N_2	Probe	7.93	8.12	0.76	8.67	13.70	1.14	1.51	
1	N_2	Winkler	7.93	8.12	0.76	8.67	15.06	1.25	1.65	
2	$SO_3^=$	Probe	8.1	8.12	0.76	8.62	13.32	1.11	1.46	
2	$SO_3^=$	Winkler	8.1	8.12	0.76	8.62	17.41	1.43	1.89	
3	N_2	Probe	6.9	7.11	0.76	8.67	14.26	1.19	1.56	
3	N_2	Winkler	6.9	7.11	0.76	8.67	19.16	1.57	2.06	
4	$SO_3^=$	Probe	6.99	7.04	0.73	8.61	13.05	1.09	1.50	
4	$SO_3^=$	Winkler	6.99	7.04	0.73	8.61	17.27	1.42	1.95	
5	N_2	Probe	5.93	5.99	0.73	8.62	14.53	1.21	1.66	
5	N_2	Winkler	5.93	5.99	0.73	8.62	22.04	1.78	2.44	
6	$SO_3^=$	Probe	6.0	6.01	0.76	8.59	14.45	1.21	1.59	
6	$SO_3^=$	Winkler	6.0	6.01	0.76	8.59	—	—	—	
6B	$SO_3^=$	Probe	6.0	6.09	0.73	8.59	14.16	1.18	1.62	
6B	$SO_3^=$	Winkler	6.0	6.09	0.73	8.59	16.24	1.34	1.84	
7	N_2	Probe	4.0	4.0	0.73	8.62	17.62	1.45	1.98	
7	N_2	Winkler	4.0	4.0	0.73	8.62	21.86	1.77	2.42	
8	$SO_3^=$	Probe	4.0	4.0	0.73	8.54	17.77	1.46	2.00	
8	$SO_3^=$	Winkler	4.0	4.0	0.73	8.54	13.86	1.16	1.58	
1	N_2	Winkler	9.0	9.0	0.73	8.60	33.78	2.60	3.54	0.01
1	N_2	Probe	9.0	9.0	0.73	8.60	17.82	1.46	2.00	
2	$SO_3^=$	Winkler	8.9	8.9	0.73	8.56	29.65	2.32	3.16	
2	SO_3	Probe	8.9	8.9	0.73	8.56	17.55	1.44	1.97	
3	N_2	Winkler	8.2	8.2	0.73	8.58	21.75	1.76	2.40	
3	N_2	Probe	8.2	8.2	0.73	8.58	17.48	1.44	1.96	0.01
4	$SO_3^=$	Winkler	8.2	8.4	0.73	8.54	24.95	1.99	2.71	
4	$SO_3^=$	Probe	8.2	8.4	0.73	8.54	17.55	1.44	1.97	
5	N_2	Winkler	5.0	5.2	0.73	8.52	33.03	2.55	3.47	0.08
5	N_2	Probe	5.0	5.2	0.73	8.52	17.31	1.42	1.94	
6	$SO_3^=$	Winkler	5.0	5.2	0.73	8.49	17.89	1.47	2.00	0.19
6	$SO_3^=$	Probe	5.0	5.2	0.73	8.49	14.4	1.20	1.64	

only for test waters with very similar water quality characteristics.

Recommendations

The chemical properties of the test water should be specified in terms of alkalinity, pH, iron and manganese.

If the Winkler DO titration method is used to determine oxygen transfer by the non-steady state technique, testing should be conducted at pH 6.9 in a 0.01 molar phosphate buffered system (1.21 g/l NaH_2PO_4 + 1.43 g/l Na_2PO_4) by the addition of 100 g/l Na_2SO_3 as $SO_3^=$ and 2 mg/l cobalt chloride hexahydrate as CO^{++}. The total iron in the test water should not exceed 0.3 mg/l, and the total manganese should not exceed 0.05 mg/l.

Further work is necessary to quantify the enhancement effects of iron, manganese, and other transitional elements at concentrations less than 2 mg/l so that appropriate corrections can be made to oxygen transfer data obtained during testing with different natural water systems.

Some sources of reagent grade sodium sulfite contain up to 200 mg/l of iron and other transitional elements. These materials should not be used to deoxygenate test water. The iron may cause substantial oxygen transfer enhancement after 5-10 successive deoxygenations.

Acknowledgements

The authors gratefully acknowledge Mr. O. E. Albertson for initiation and review of this work. We also gratefully acknowledge Mr. Robert W. Okey, Mr. David DiGregorio and Mr. Dan McCarthy for reviewing and for the assistance in editing this paper. This paper was presented by Mr. Dan McCarthy at the Workshop.

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Discussion

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Tests of a fine-bubble diffused-air system, conducted the Water Research Centre's laboratory at Stevenage and at field sites in other locations in the UK, have shown that reproducible results can be obtained, provided that the tests are carried out under carefully controlled conditions (particularly with a clean tank and aerator) in clean water with and without addition of detergent. Changes in the hardness of the water and small changes in concentrations of inorganic salts, including sodium sulphate content, as a result of conducting several tests in the same water, had little effect on the results. Addition of detergent (5 mg/l) did affect the accuracy with which the end-point of the Winkler titration could be detected when using starch as an indicator, but the error was generally less than 5%.

In six replicate tests in clean water, the rate of oxygen transfer for a fine-bubble aeration system, measured using six dissolved-oxygen electrodes sited at different positions in the water, varied by only 8% of the mean value. The average value measured using the electrodes was only 2% different from the rate measured by taking 'grab' samples from near the surface of the water and analyzing them for dissolved oxygen using the modified Winkler Method described by Montgomery *et al.**

*Montgomery, H.A.C., N.S. Thom, and A. Cockburn. "Determination of Dissolved Oxygen by the Winkler Method and the Solubility of Oxygen in Pure Water and Sea Water." Journal Applied Chemistry, Vol. 14, p. 280, 1964.

Closure

Daniel A. McCarthy

I believe that Dr. Boon's verbal comments during the discussion period were concerned with three primary areas: (1) his preference for DO probes (as opposed to Winkler titrations) as the means of monitoring dissolved oxygen during the non-steady state sulfite reaeration test, (2) his experience that "changes in the hardness of the water and small changes in concentrations of inorganic salts, including sodium sulfate content, as a result of conducting several tests in the same water, had little effect on the results", and (3) his belief that the addition of detergent (up to 5 mg/l) to the water used during the non-steady state sulfite reaeration test would yield more realistic and more useful results than are ordinarily obtained with the standard test.

The DO probe versus Winkler titration debate has been ongoing for several years. Advocates of probes generally state that they yield "more precise" and "more accurate" results than Winkler DO titrations. The "more accurate" statement arises from the many known interferences to the Winkler titration method. These interferences were summarized in our paper presented at Asilomar. These interferences can be markedly reduced by employing a phosphate buffered system maintained at a pH of 6.9 or less. The exact phosphate buffer requirements were summarized in the paper.

The opponents of Winkler titration procedures do not generally address the idiosyncratic behavior of DO probes. During the experiments I discussed at Asilomar (Table 3) DO probes were affected to a substantial degree by the presence of iron and manganese contaminants in the test water. The effects of similar iron and manganese concentrations on mass transfer coefficients, K_La values, determined by Winkler techniques, were not as pronounced as the effects apparent with DO probes. Results obtained with DO probes were essentially the same as results obtained with Winkler titrations when the liquid system was phosphate buffered and adjusted to a pH less than 6.9.

The precision of K_La data we have obtained with DO probes has been similar to the precision of K_La data we have obtained by Winkler procedures. Generally, the standard deviation of the K_La data obtained by either method has been equal to about 7% of the mean K_La values obtained during bench-, pilot-, or full-scale aeration testing. The acquisition of probe data in the field has presented numerous difficulties. Each probe membrane usually has to be replaced several times each day. This procedure is time consuming and therefore costly. Strip chart recorders are often used during field performance tests that require the use of probes for DO determinations.

Failure of a probe membrane, transmission cord, DO meter, or strip chart recorder could abort a performance test. The probe system must be calibrated. The Winkler procedure is employed for this purpose. In our experience the use of probes has yielded K_La data essentially the same as data obtained by Winkler procedures in phosphate buffered systems maintained at a pH of 6.9 or less. The probes have proved to be cumbersome to use in the field. Probe system malfunctions have caused performance tests to be aborted. These interruptions and delays have been costly in both time and money. We prefer to use the Winkler technique to avoid these delays.

Dr. Boon's statement concerning his experience that "changes in the hardness of the water and small changes in the concentrations of inorganic salts, including sodium sulfate content, as a result of conducting several tests in the same water, had little effect on the results" is contrary to the experience we have had with probes and Winkler titrations over the years. Generally, measured values of K_La have increased as the hardness, pH, and sulfate content of the test water have increased. An accurate prediction of the magnitude of the increase as a function of these factors is masked by the interactive effects of the factors. The effects are most pronounced, for a given test water, when five or more sulfite additions have been introduced to the test basin.

Dr. Boon's belief that the addition of detergent (up to 5 mg/l) to the test water used during the non-steady state sulfite reaeration test would yield more realistic and more useful results is sure to generate debate among persons interested in aeration test procedures. The conventional wisdom states that the addition of detergent to bubble aeration systems will depress measured values of K_La while the addition of like amounts of detergent to surface aeration systems will enhance measured values of K_La. The effects of detergent (which one?, what concentration?) on probe and Winkler K_La determinations for a variety of natural water systems would have to be quantified before I could support the inclusion of the addition of detergent to the test water in any proposed aeration testing standard.

Gas Flow and Power Measurement

16

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Gas Flow Measurement

Gas flow measurement is a fundamental part of oxygen transfer testing. The gas flow measurement section of this paper will discuss some of the basic types of primary and secondary flow elements and will attempt to show some of the advantages and disadvantages of each. Primary emphasis, however, will be on a complete discussion of the concentric orifice plate and its application to air flow measurement. An attempt has been made to draw upon as much personal experience as possible. In particular, a special section of the paper discusses a recent experience with blower pulsation and its effect on air flow measurement.

Two references used in the writing of the "Gas Flow Measurement" section of this paper deserve special mention. L. K. Spink's *Principles and Practices of Flow Meter Engineering* (13) was used very extensively in the sections dealing with the general discussion of primary and secondary flow elements. Also, C. F. Cusick's *Flow Meter Engineering Handbook* (1) was used widely in the sections dealing with the orifice plate. Both references were referred to at a number of points throughout the paper and are highly recommended for anyone interested in making flow measurements.

Primary Flow Elements

There is a wide variety of commercially available flow measurement devices. According to Spink (13), most of these can be referred to as head meters, although there are a considerable number that operate on entirely different principles.

Head Meters

Head meters convert rate of flow into a differential pressure, a mechanical force, or a change of head or level (over a weir or flume). This category of meters includes the orifice plate, Venturi tube, flow nozzle, "Lo-Loss" measuring tube, pitot tube (and its variations), pipe elbow tap, target meter, weir, flume, and others.

Head meters have a number of advantages (13). First of all, they are usually simple and easily reproducible. Secondly, flow can be accurately determined without the need for an actual fluid flow calibration of the primary measuring device. Flow rates can be determined by merely knowing the dimensions of the primary device and the properties of the flowing fluid. Furthermore, only well established and easily maintained tolerances are critical in head meters. For the special case of orifice plates, only the pipe and orifice diameters are critical. Other parameters such as concentricity of the orifice in the pipe, location of the differential pressure taps, and degree of smoothness of the pipe itself, are not too critical. Any corrosive damage to the orifice bore can be easily seen by removing the plate from the line. A further advantage of head meters is that the differential pressures they produce are very accurately reproducible under the same set of conditions.

Head meters operate on the principle that the rate of flow of fluid is proportional to the square root of the pressure differential. Furthermore, for a given mass flow rate, the reading of the head meter varies inversely as the square root of the density. Head meters are also affected by viscosity, relative pipe size, and velocity. The Reynolds number, however, serves to correlate these parameters with the proper meter coefficients.

Orifice Plates

The orifice plate is probably the most common type of head meter. It is essentially a thin plate in the flow through which a sharp, square-edged orifice has been bored. The ratio of the orifice diameter to the inside pipe diameter is known as the "diameter," "orifice," or "beta" ratio. An orifice has a number of variations. Generally speaking, an orifice is either concentric, eccentric, segmental or annular. The concentric orifice is by far the most widely used. The other variations were derived to handle flows containing solids in suspension and water vapor. Orifice plates have many advantages, including accuracy, simplicity, cost-effectiveness, versatility, and ruggedness. A disadvantage is that they sometimes involve a relatively high permanent pressure loss.

Venturi Tubes

Venturi tubes are used to measure flows when a relatively low permenent pressure loss is required. Usually they will handle about 60% more flow than an orifice plate, but their permanent pressure loss is only 10 to 20% of the differential pressure (1). They are also good for measuring flows with solids in suspension. Furthermore, for the measurement of a given air flow, they can require less upstream straight pipe than an orifice plate. Venturi tubes consist of a converging and diverging nozzle. The diverging section tends to make the pressure recovery efficient so that the permanent pressure loss is low. The Venturi works on the basic principle that the change in static pressure between two regions of different cross sectional area is proportional to flow. Static pressure readings are taken in an upstream region of the Venturi and at the throat. Generally speaking, Venturi meters are relatively expensive and somewhat large but in many situations this is more than offset by their advantages.

Flow Nozzles

Flow nozzles have advantages similar to Venturi tubes. They consist of a converging nozzle only. While the pressure recovery is not as efficient as with the Venturi tubes, it is still much better than with orifice plates. They will handle about 60% more flow than an orifice plate with a permanent pressure loss that varies from 30 to 80% of the differential pressure, depending on the diameter ratio (1). The principle of operation is similar to that of the Venturi tubes. Static pressure readings are taken in an upstream region of the flow nozzle and at the throat. Generally speaking, flow nozzles are less expensive than Venturi tubes but are considerably more expensive than orifice plates.

Pitot Tubes

The pitot tube or one of its variations, the pitot-static tube or the Annubar (a commercially available device), provides a very low permanent pressure loss. Essentially, they are in-line pressure probes that measure velocity head. All variations of the pitot tube measure the stagnation pressure of the flow, which is equal to the static pressure plus the velocity head. The pitot tube requires a separate measurement of static pressure, while the pitot-static tube has a static pressure tap in the side of the stagnation pressure probe. In both cases, the difference between the stagnation pressure and the static pressure is the velocity head.

The pitot tube and the pitot-static tube are limited by the fact that they essentially measure velocity at one point in the flow. Normally this can be related to the average pipe velocity if the flow conditions are steady; however, upstream pipe disturbances can significantly alter this relationship. The Annubar, on the other hand, measures an average stagnation pressure across the pipe diameter and does a better job of estimating the average pipe velocity. Furthermore, the Annubar differs from the pitot-static tube in that it measures a differential pressure that is somewhat greater than the velocity head due to a slight suction condition created at the low pressure tap. A disadvantage to the pitot tube, pitot-static tube, and the Annubar is that they may sometimes require a pipe size reduction in order to increase the velocity head since velocity heads in normal air flow applications are usually low.

Other Types of Meters

There are a number of other types of meters available for flow measurement. Among these are magnetic meters, turbine meters, variable area meters (rotameters), mass flow meters, rotary lobe gas meters, and others. These meters are good for specific applications and with the exception of the rotameter, are generally more complex and costly than the head meters. For gas flow measurement, however, it would seem that only rotameters and rotary lobe gas meters have wide application.

Rotameters

The rotameter is actually a variable orifice head meter. It consists of a float in a vertical transparent tube. The inside diameter of the tube is linearly tapered from top to bottom, with the widest portion at the top. Flow enters the tube at the bottom, and the float rises until the weight of the float just balances the lift generated by the buoyancy of the float and the pressure differential across it. The headloss through the rotameter is essentially constant and usually fairly low. Furthermore, the rotameter scale is nearly linear with flow. The combination of these factors makes it exceptionally desirable for measuring flows that vary over a wide range (i.e., 10 to 1). Furthermore, the rotameter is not affected significantly by upstream piping disturbances; no straight lengths of pipe are generally required. It is especially suitable for pipe sizes up to 3 in. (14).

Rotary Lobe Gas Meters

The rotary lobe gas meter is a positive displacement device. It is highly recommended for measuring the flows from positive displacement blowers and other pulsating equipment because it is not affected by pulsation as are the head meters. Volume flow rate is determined by measuring the rpm developed as the air passes through the meter. Rotary lobe gas meters are usually expensive, but where high accuracy is required, they are exceptionally good.

In summary, there are a large number of accurate gas flow measurement devices. Each has its own advantages and disadvantages, and one may be better suited to a particular application than another. Holman (8) shows a comparison of the operating characteristics of various flowmeters, according to Miesse and Curth. For a more detailed analysis of the various primary flow elements, the reader is referred to Spink (13).

Secondary Flow Elements

Secondary flow elements are used to measure the output of the primary flow elements. For head-type primary flow elements, this output is usually a differential pressure. While there are a wide variety of differential pressure measuring devices, they can be generally classified into two basic groups according to whether or not the fluid which exerts the differential pressure is in direct contact with an indicating fluid (13).

Wet Meters

According to Spink (13), wet meters are devices where the fluid which exerts the differential pressure is in direct contact with the indicating fluid. This group of meters can be further divided into the liquid and liquid seal varieties.

Liquid Meters

With liquid meters, a difference in liquid level is developed between the high and low pressure sides of the device. Liquid meters are the oldest, simplest, and generally speaking, the most accurate and reliable of all differential pressure measuring devices. They are excellent in applications where only visual indication is needed and where the static pressures and the nature of the fluid are conducive to transparent tubes. Various types of liquid manometers are available, including the U tube, well type, and mercury float type. Of these, the U tube manometer is the simplest, the differential pressure being the distance between the water columns in each leg. Well manometers are similar to U tube manometers except that the high pressure leg of the device is actually a large reservoir. Since the volume of the reservoir is very large relative to the volume of the manometer indicator tube, it is only necessary to read one leg of the manometer; the fluid level in the reservoir does not change appreciably. An important variation on the well type manometer is the inclined well-type manometer. Inclining the manometer helps to expand the indicator scale so that differential pressure can be read more accurately. The mercury float type of liquid manometer is similar to the other liquid meters except that a float attached to an indicating needle follows the mercury surface on the high pressure side. This type of meter is very accurate, although relatively expensive, and for many years it was the accepted standard meter for orifice flow measurements.

Liquid-Seal Meters

With liquid seal meters, the differential pressure is determined by the displacement of a piston-like device, across which the differential is applied and which acts against the tension of a spring. The meter fluid acts like a seal between the high and low pressure sides of the device. An indicating needle senses the position of the piston. Liquid seal meters are well suited to the measurement of low differential pressures. Generally speaking, they are used where simple, self powered mechanisms with considerable output power are required. Varieties of this type of meter include the cylindrical inverted bell meter, the Ledoux inverted bell meter, and the ring balance meter (similar in principle to the bell-type meters).

Dry Meters

According to Spink (13), dry meters are devices where the fluid which exerts the differential pressure is not in contact with an indicating fluid. Dry meters are becoming increasingly popular wherever indication, recording, integration, and/or transmission are required. They are relatively inexpensive compared to the mercury float-type meter. Dry meters are of two basic types: motion and force balance.

Motion-Type Meters

The motion-type dry meter consists of a bellows system across which the differential pressure is applied; the resulting force deflects a spring, and the motion provides the output. This type of meter is well suited to direct indication, recording, and integration.

Force Balance-Type Meters

The force balance-type of meter employs a diaphragm, against which the differential pressure is applied. An electric current or pneumatic pressure develops a second force, and the two forces are made to oppose each other. A sensitive motion detector detects any imbalance in the forces and corrects the second force until they are balanced. The magnitude of the current or pressure of the second force is then directly proportional to differential pressure. This type of meter is used primarily as a pressure transmitter to a remote location.

As with primary devices, the selection of a specific secondary device for a given application depends on a number of factors. Generally speaking, where visual indication is sufficient, the liquid manometers are suitable as they are simple, low cost, rugged, and accurate. Other needs may dictate a requirement for other types. For a much more detailed discussion of various secondary devices and their specific application, the reader is referred to Spink (13).

Required Measurements for Gas Flow Determinations

A number of measurements are required for accurate gas flow determinations. As a minimum, the following readings should be taken:

1. differential pressure (with most head meters), h
2. flowing gas temperature, T_f
3. flowing gas pressure (static pressure), p_f
4. ambient temperature, T_a
5. ambient pressure, p_a
6. ambient relative humidity, RH

Other readings may be necessary on specific types of flow meters.

The differential and static pressure readings should be made with a manometer of some type. Unless flow totalization or recording is required, a simple U tube or well-type manometer works very well. Pressure gauges can be used for static pressure determinations, but experience has shown that unless these gauges are of the oil-filled variety or are at least protected with a pulsation dampener (snubber), they can be rendered useless in a short period of time by vibration and pressure oscillation. A mercury-filled manometer is more rugged and accurate.

The taps for both the differential and static pressure readings should be constructed in a similar fashion to the tap shown in Figure 1 (see also the "Orifice Plate Construction and Installation" section of this paper).

Flowing gas temperature can be read with a bimetallic dial thermometer that is mounted permanently in the pipe. These thermometers can be bought with probes of varying lengths for different pipe sizes. Where electronic determinations are required, as in temperature compensation circuits, thermistors can be employed. In any case, the probe should extend well into the center of the pipe.

Ambient temperature can be determined with any number of commercially available wall-mounted thermometers. Care should be taken to obtain these readings in the shade as direct sunlight will cause erroneously high readings due to the radiant energy of the sun.

Ambient pressure should be determined with a good quality mercurial barometer. These readings should also be taken in a shaded area and corrections should be made for the thermal expansion of the brass indicating scale, the change in mercury density with temperature, and the effect of the earth's gravitational field. Tables for making these corrections are usually included with the barometer.

Relative humidity readings should be taken with a hygrometer or a psychrometer. The sling psychrometer, in particular, is portable and inexpensive. Usually, wet and dry bulb thermometer readings are taken and are converted to relative humidity with the use of tables that accompany the device. Some hygrometers are available which read relative humidity directly.

Standard Conditions

Since gases are compressible fluids, and in some instances may contain varying amounts of water vapor, the ratio of mass flow to volumetric flow is not a constant, but depends on the temperature, pressure and water vapor partial pressure. The need to relate mass flow to a volumetric flow by a constant, as in incompressible flow, gave rise to the term "scfm," standard cubic feet per minute. This refers to an imaginary volumetric flow rate; it is that flow which would exist if the same mass flow of dry gas existed at standard conditions of temperature, pressure, and relative humidity. This volumetric flow rate can be multiplied by a constant factor to obtain the mass flow of dry gas, or by a different constant factor to obtain the mass flow of gas at standard humidity.

Standard conditions for air flow measurement are usually taken as 68°F (20°C, 528°R), 14.70 psia (1 atm, 760 mm Hg) and 36% relative humidity. Standard temperature is sometimes taken as 60°F, particularly by the natural gas industry. Blower manufacturers and others, on the other hand, sometimes use 70°F. It is felt that 68°F is the most commonly used temperature standard in the oxygen transfer field, and it has been selected for use in this paper. It should also be pointed out that for gases other than air, the relative humidity standard is zero rather than 36%.

Orifice Plate Discussion

It would be virtually impossible to discuss all the various types of flow meters in detail in the context of a single paper. With this in mind, a decision has been made to concentrate on the application of the concentric orifice plate. It is certainly one of the most widely used primary flow elements due to its accuracy, simplicity, cost-effectiveness, versatility, and durability. The information contained in the sections which follow should provide guidance for the design, installation, and operation of a successful orifice meter. Much of this information is also applicable to head meters in general.

Basic Orifice Plate Equation for Gas Flow

Based on Cusick (1), the basic orifice plate equation for gas flow, without refinements of any kind, can be written as follows:

$$(1) \quad Q_{approx.(b)} = (218.44/60)S_o d^2 (T_b/p_b)\sqrt{(p_f/T_f)(h/G)}$$

in which:

$Q_{approx.(b)}$ = approximate gas flow at base conditions of T_b and P_b, cfm

S_o = orifice coefficient

d = actual internal pipe diameter, in.

T_b = base temperature, °R

p_b = base pressure, psia

T_f = flowing gas temperature, °R

p_f = flowing gas pressure, psia

h = differential pressure, in. H_2O (dry)

G = dry gas specific gravity

At base conditions of 68°F and 14.70 psia, this expression reduces to:

$$(2) \quad Q_{approx.(s)} = 130.77\, S_o d^2 \sqrt{(p_f/T_f)(h/G)}$$

in which:

$Q_{approx.(s)}$ = approximate gas flow at base conditions of 68°F and 14.70 psia, cfm

The exact form of the orifice plate equation will be discussed in a later section of this paper. For preliminary design of an orifice meter, however, all that is usually required is the basic orifice plate equation.

Location of the Orifice Plate

One of the most important factors in obtaining accurate air flow measurements with any head meter is to install the primary element in the proper location. The most refined head meter equation will not compensate for an improperly located primary device. A number of references (1) (2) (13) are available which give the recommended piping requirements before and after an orifice plate for various types of pipe and fitting configurations and for various diameter ratios. These data are usually appropriate for flow nozzles as well. No attempt has been made to reproduce this material in this paper; however, certain general comments can be made.

First of all, it is good practice to install the primary element with as much upstream straight pipe as possible. Upstream fittings and curved pipe sections tend to upset the straight flow conditions that are required for good flow measurements with head meters. In particular, upstream throttled valves and configurations that tend to produce swirling gas flows require a particularly long length of upstream straight pipe ahead of the orifice meter. An example of a type of pipe configuration that leads to swirling is a series of 90° bends that are out of plane. Downstream piping conditions are important but are far less critical than upstream conditions. It is important to note that the required length of straight pipe for a given piping configuration always increases with the diameter ratio. This means that where adequate length of straight pipe is at a premium, it is a good idea to design the orifice plate with a smaller diameter ratio.

Of particular concern, due to recent experience with this problem, is locating the orifice plate so that it will not be affected by pulsation. Pulsation, as used here, refers to the high frequency pressure oscillations that are produced by positive displacement blowers, reciprocating compressors, and other similar devices (see the "Pulsation" section of this paper). If pulsation exists at the point of flow measurement, although it may not be directly obvious, it can cause substantial error in the flow determination. While there is probably no definite design criteria as yet to ensure that pulsation will not occur in a given situation, it is a good idea to follow certain basic guidelines. First of all, the orifice plate should be located as far away from the source of the disturbance (positive displacement blower, etc.) as possible. The combination of pipe volume and headloss between the source and the primary element tends to damp out the high frequency oscillations (13). It may be desirable to install a large reservoir or an artificial headloss upstream of the orifice plate for this purpose. Recently a reservoir was installed in the blower discharge piping for the Los Angeles County Sanitation Districts' aeration equipment evaluation program. This has corrected a very substantial pulsation problem.

It is also good practice to isolate the orifice plate as much as possible from the low frequency pressure oscillations that may occur as a result of water level variations in an aeration tank due to mixing. Again, pipe volume or headloss between the primary element and the air release point can help alleviate this problem.

Another point of concern when locating an orifice plate is installing it at a point where there is no danger of water infiltration. Water from an aeration tank or other source can get into the manometer lines and make the differential pressure readings completely inaccurate. Adequate precautions should be taken to ensure that this does not occur.

Line Size Determination

One of the first requirements in selecting the proper line size is to know the range of flow rates to be encountered. It should be kept in mind that a given orifice installation will usually be able to handle only a 4 or 5 to 1 range in flow rates. This is due primarily to accuracy limitations imposed by the secondary device as to readability at low flows and to permanent headlosses associated with the primary device at high flows. The Reynolds number at low flows may also be a problem, however. In any case, if a wide range of flow rates is to be encountered, it is a good idea to consider using several pipe lines of different sizes. It is also possible, however, to use different orifice plates in the same line size, but this can be done only up to a point. Eventually, the diameter ratio may fall outside the limits mentioned earlier in the section on orifice plate taps or the orifice plate and/or pipe headlosses may become excessive. Furthermore, it is possible that the pipe Reynolds numbers at low flows may fall out of the turbulent flow regime. If the flow is not turbulent, the orifice plate coefficients may vary significantly with Reynolds number; this is undesirable.

The line size determination should be based initially on considerations of pipe headloss. Care should be taken so that pipe losses at the highest air flows are not too high. A number of references describe the estimation of pipe and fitting headloss (3) (6) (9) (10). Maximum headloss should be estimated using the worst conditions of temperature and pressure. It should be remembered when determining the line size that there will be an additional headloss due to the orifice plate. A number of references (1) (2) (13) discuss the estimation of orifice plate headlosses.

After a tentative selection of line size has been made, the pipe Reynolds number at low flow should be checked. According to Cusick (1), the pipe Reynolds number can be calculated from the following formula:

$$R_e = 28.50\,(Q_{dgs}G)/(d\mu) \qquad (3)$$

in which:

R_e = pipe Reynolds number

Q_{dgs} = dry gas flow at 68°F and 14.70 psia, cfm

μ = gas absolute viscosity, cps

For air, this expression reduces to:

(4) $$R_e = 28.50\ Q_{dgs}/d\mu$$

For most Reynolds number considerations, it is not usually necessary to consider the effects of relative humidity. The turbulent flow regime is sometimes considered to exist above pipe Reynolds numbers of 10,000, and it is generally desirable to exceed this number at low flow. To aid in the calculation of Reynolds numbers, the following expression for absolute viscosity can be used (10):

(5) $$\mu = (161 + 0.28\ T_f) \times 10^{-4}, \text{cps}$$

Selection of Orifice Plate Taps

There are a number of different types of orifice plate taps. The most widely used are flange taps. These are located 1 in. from the upstream face of the orifice plate and 1 in. from the downstream face of the orifice plate. Normally, special flanges are purchased with these taps already in place; this makes the orifice plate installation considerably easier. Furthermore, since the taps are symetrically located, the orifice may be used to read reversing flows (13). For flange taps on pipe sizes 4 in. and above, the diameter ratio must be between 0.10 and 0.75; on pipe sizes 3½ in. and smaller, the diameter ratio must be between 0.20 and 0.70 (1). Flange taps should not be used on pipe sizes smaller than 2 in. (13). The coefficients for concentric orifice plates with flange taps have a tolerance of ±0.55% for diameter ratios from 0.20 to 0.70 on pipe sizes 2 in. and larger (1). The term tolerance, as used here, is equivalent to twice the standard deviation and is the maximum error that would be experienced in 95% of all installations (13). Extensive tables of flange tap coefficients can be found in references (1) and (7).

Vena contracta taps are also widely used. These taps are nominally located 1 pipe diameter upstream and 1/2 pipe diameter downstream. The actual downstream location varies with the diameter ratio. The exact location of the vena contracta is as shown by Cusick (1). Cusick also shows the correction factors for variations from the exact location. On 4-in. pipe sizes and over, vena contracta taps can be used as long as the diameter ratio is between 0.10 and 0.80 (1). Vena contracta taps should not be used on pipe sizes smaller than 4 in. due to the interference between the downstream flange and the downstream tap. The coefficients for concentric orifice plates with vena contracta taps have a tolerance of ±0.50% for diameter ratios between 0.20 and 0.70 (1). Special orifice plate flanges are not required when using vena contracta taps; instead, standard pipe flanges are used and the taps are installed in the pipe wall. Extensive tables of vena contracta coefficients can be found in references (1) and (7).

Corner taps are used in place of flange taps on pipe sizes smaller than 2 in. These taps are located in the corner between the pipe wall and the edge of the orifice plate in both the upstream and downstream locations. Using flange taps on pipe sizes smaller than 2 in. with high diameter ratios would place the downstream pressure tap downstream of the vena contracta. The region downstream of the vena contracta is a very turbulent zone for which standard coefficients do not apply. Special flanges or orifice holding rings can be purchased when corner taps are used. Sometimes accuracy can be improved by modifying the corner tap method such that the upstream tap is located one diameter upstream of the orifice plate (13).

Radius taps are very similar to vena contracta taps and are located at exactly 1 pipe diameter upstream and 1/2 pipe diameter downstream from the inlet face of the orifice plate.

Pipe or full flow taps are located 2½ pipe diameters upstream and 8 pipe diameters downstream of the inlet edge of the orifice plate. Flow measurement with pipe taps is not as accurate as with flange or vena contracta taps with the same orifice plate. The probable error of measurement is about 50% greater (13). Furthermore, pipe taps require greater lengths of straight pipe. They do, however, permit the measurement of flows in pipe sizes smaller than 2 in. using standard pipe flanges. The diameter ratio limits for pipe tapes are the same as for flange taps (1).

In summary, for orifice plate measurements on pipe sizes up to 2 in., either corner taps or full flow taps would be the method of choice. On pipe sizes between 2 in. and 4 in., flange taps should probably be used. On pipe sizes of 4 in. and larger, either flange or vena contracta taps should probably be used.

Selection of Differential Pressure Range

Before an orifice plate can be designed, it is first necessary to determine the desired range of differential pressures for the given range of gas flow rates. Generally speaking, it is good practice to keep the differential pressure from becoming either too high or too low. Very low differential pressure readings may make the flow determination less accurate since the error in reading the manometer may be very significant in terms of flow. Furthermore, if there are any pulsation effects in the air line, low differential readings will give a proportionally greater error in flow rate determination. On the other hand, very high differential pressure readings can result in very high permanent headlosses associated with the primary device. While there is a wide range of acceptable differential pressures, unless there is a specific reason why a particular range should be selected, it is a good rule of thumb to let the differential in inches of H_2O equal the static pressure in psia (13). This might normally be done at average flow conditions as long as the range of flows is not too great. A check should then be made to ensure that the error involved in reading the manometer at low flows and the permanent headloss from the orifice plate at high flows are acceptable.

Diameter Ratio Determination

Once tentative decisions have been made concerning desired pipe size and orifice plate differentials, it is possible to calculate an orifice plate coefficient, S_o. By rearranging the basic orifice plate equation for gas flow at standard conditions (equation 2), the following expression for S_o is obtained:

(6) $$S_o = (7.647 \times 10^{-3})\,(Q_{approx.(s)}/d^2)\sqrt{(T_f/p_f)(G/h)}$$

S_o can be calculated by using the average Q, the corresponding h, the actual d, and estimated values for T_f and p_f. The next step is to calculate the pipe Reynolds number for these condition by using Equation 4. The reader is referred at this point to orifice plate coefficient tables for the type of orifice plate taps employed (1)(7). By referring to the proper pipe size at the proper pipe Reynolds number, it will be possible to determine the diameter ratio that corresponds to the calculated orifice plate coefficient.

At this point, it is important to verify that the diameter ratio determined is within the range of diameter ratios for which the reported tolerances apply (see the "Selection of Orifice Plate Taps" section of this paper). Furthermore, the diameter ratio so calculated should be compatible with the existing lengths of straight pipe for the proposed air measurement system. Also, the permanent headloss associated with the orifice plate at maximum flow should be acceptable. If the diameter ratio does not satisfy all three of these conditions, then it will be necessary to modify the differential range, the line size, or any of the other variables over which there is control, until it does.

Exact Orifice Plate Equation for Gas Flow

The exact orifice plate equation for gas flow is:

(7) $$Q_{eb} = Q_{approx.(b)}\, Y\, F_a\, F_{pe}\, F_{wv}\, F_m\, F_{vc}\, F_{pv}$$

in which:

Q_{eb} = exact gas flow at base conditions of T_b, p_b and $(RH)_b$ (the relative humidity at base conditions)

Y = gas expansion factor

F_a = orifice area correction factor

F_{pe} = pipe expansion correction factor

F_{wv} = relative humidity correction factor

F_m = manometer correction factor

F_{vc} = vena contracta correction factor (for vena contracta taps only)

F_{pv} = super-compressiblility correction factor

For the special case of air at standard base conditions of 68°F, 14.70 psia, and 36% relative humidity, $Q_{eb} = Q_a$ = the air flow at standard conditions. An explanation of the factors in Equation 7 is given in the discussion which follows:

Gas Expansion Factor

The gas expansion factor, Y, accounts for the expansion of the gas as it passes through the orifice. It is most significant in low pressure systems with high differential pressures. According to Cusick (1), the expansion factor for flange and vena contracta taps can be expressed by the following equation:

(8) $$Y = 1 - [0.41 + 0.35\,(d_o/d)^4]\,(x/k)$$

in which:

d_o = orifice bore diameter, in.

x = (differential in psi)/(upstream flowing pressure in psia)

k = ratio of gas specific heats, C_p/C_v (use 1.395 for air)

C_p = specific heat at constant pressure

C_v = specific heat at constant volume

Orifice Area Correction Factor

The orifice area correction factor, F_a, compensates for the thermal expansion of the primary element at temperatures other than 68°F (1)(13). The following relationship has been developed from data derived from Spink (13) and is applicable to Type 304 or 316 stainless steel:

(9) $$F_a = (1.699 \times 10^{-5})\,T_f + 0.9910$$

This equation is valid over the range of temperature to be encountered in normal aeration practice.

Pipe Expansion Correction Factor

The pipe expansion correction factor, F_{pe}, compensates for the thermal expansion or contraction of the pipe at temperatures other than 68°F. The following relationship has been developed from data derived from Spink (13) and is applicable to steel pipe:

(10) $$F_{pe} = (1.333 \times 10^{-5})\,T_p + 0.9930$$

in which:

T_p = pipe temperature, °R

For an approximate calculation, it may be reasonable to assume that $T_p = T_f$. This equation is also valid over the range of temperatures to be encountered in normal aeration practice

Relative Humidity Correction Factor

The purpose of the relative humidity correction factor, F_{wv}, is two-fold (1) (13). First, it corrects the specific gravity in the flow equation from that of dry gas to that of the actual wet gas. Second, it converts the actual wet gas flow obtained into an equivalent gas flow at base humidity. This means that the mass flow rate of dry gas is the same in both cases. The expression for F_{wv} is:

$$(11)\quad F_{wv} = [1-(p_w/p_f)]/\{[1-(p_{bw}/p_b)]\sqrt{1+(p_w/p_f)[(G_w/G)-1]}\}$$

in which:

p_w = partial pressure of water vapor in the gas line, psia

p_f = flowing gas pressure, psia

p_{bw} = vapor pressure of water at base conditions, psia (0.1221 psia at standard conditions of 68°F, 14.70 psia, and 36% relative humidity)

p_b = base pressure, psia (14.70 psia at standard conditions)

G = dry gas specific gravity (1.0 for air)

G_w = water vapor specific gravity (0.6225)

Substituting the known quantities for air at standard base conditions yields:

$$(12)\quad F_{wv} = 1.0084[1-(p_w/p_f)]/\sqrt{1-0.3775(p_w/p_f)}$$

In the case of a blower taking suction in ambient air, p_w can be calculated from the ambient barometric pressure, ambient relative humidity, vapor pressure of water at ambient temperature, and flowing gas pressure, as follows:

$$(13)\quad p_w = (p_{wa}\, p_f)/p_a', \text{ (assumes no condensation in the air line)}$$

$$(14)\quad p_{wa} = RHp_{vpa}/100$$

in which:

p_{wa} = ambient partial pressure of water vapor in the air, psia

p_a = ambient barometric pressure, psia

p_{vpa} = vapor pressure of water at ambient temperature, psia

RH = ambient relative humidity, %

According to Sawyer and McCarty (12), the vapor pressure of water at ambient temperature follows the Rankine expression of the form:

$$(15)\quad \log(p_{vpa}) = A/T + B\log(T_a) + C$$

in which A, B, and C are constants.

With the use of this formula and the vapor pressure data between 10 and 30°C of Sawyer and McCarty (12), the constants A, B, and C have been determined. Substitution of these constants into Equation 15 yields:

(16)

$$\log(p_{vpa}) = -4985.8/T_a - 3.5895\log(T_a) + 18.747, \text{ psia}$$

Manometer Correction Factor

The manometer correction factor, F_m, corrects for the density of the fluid column displacing the manometer fluid in a manometer. The following expression holds true when the displacing fluid is a gas:

$$(17)\quad F_m = \sqrt{1-(\gamma_{df}/\gamma_m)} = \sqrt{1-2.702[G_{df}F_{pv}^2 p_f/(\gamma_m T_a)]}$$

in which:

γ_{df} = density of the manometer displacing fluid, lb/ft^3

γ_m = manometer fluid density, lb/ft^3

G_{df} = specific gravity of the manometer displacing fluid

F_{pv} = super-compressibility correction factor

T_a = ambient temperature, °R

For air as the displacing fluid, mercury as the manometer fluid, and taking $F_{pv} = 1.0$:

$$(18)\quad F_m = \sqrt{1-(3.194\times10^{-3})(p_f/T_a)}\ \text{(mercury)}$$

For air as the displacing fluid, water as the manometer fluid, and taking $F_{pv} = 1.0$:

$$(19)\quad F_m = \sqrt{1-(4.336\times10^{-2})(p_f/T_a)}\ \text{(water)}$$

Vena Contracta Correction Factor

The vena contracta correction factor, F_{vc}, is used only with vena contracta taps. It corrects for the slight mislocation of the downstream pressure tap from the vena contracta. Cusick (1) has shown the correlation between F_{vc} and the downstream tap location and the diameter ratio. He shows curves of constant correction factor, F_{vc}, plotted against the low pressure tap location and the diameter ratio (d_o/d). From these curves, a curve of correction factor versus downstream tap location can be plotted for a particular orifice plate. The vena contracta correction factor can then be read directly from this curve for the given downstream tap location.

Super-Compressibility Correction Factor

The super-compressibility correction factor, F_{pv}, corrects for deviations from the ideal gas law, particularly at high pressures (i.e., 100 psi)(1) (13). It is usually insignificant at the low pressures encountered in most oxygen transfer applications.

The correction factors just discussed, together with the basic orifice plate equation, constitute the exact orifice plate equation for gas flows. Depending on actual conditions, some of these factors may be insignificant and may be disregarded without appreciable loss of accuracy. In any case, after the initial orifice plate design with the basic equation, a check should be made using the exact equation to ensure that the differential pressures for the various flows to be encountered are still acceptable. Furthermore, the exact equation should be used with the extreme flowing and ambient conditions to determine the maximum differential pressure to be encountered. This will help in the selection of a manometer with an adequate differential pressure range. Where maximum accuracy is required, air flow determinations should be made using the exact form of the equation.

Graphs and Computerization of Flow Equations

Until recently, calculating gas flow rates from measurements taken in the field could be somewhat laborious. Now, with the advent of programmable calculators, this task is much easier. The gas flow equation in its most exact form can be programmed into the calculator and readings can be converted into flows in a matter of seconds.

A particular advantage of programming the gas flow equation is that the effect of the S_0 variation with Reynolds number can be taken into account. Normally, it is possible to program the S_0 variation into the calculator. When a gas flow determination is required, an initial assumption for S_0 is made which results in an approximate air flow. This flow is then used to calculate a pipe Reynolds number, which is in turn used to calulate a more accurate S_0. Normally, this value of S_0 is very close to being exact.

It is still very useful to make graphs of air flow versus differential pressure for various flowing temperatures and pressures. These graphs should be nearly straight lines when plotted on log-log paper. They find particular application for approximate air flow determinations in the field.

Orifice Plate Construction and Installation

The orifice plate is usually made out of Type 316 or 304 stainless steel. Its construction is basically very simple; however, certain guidelines should be strictly adhered to. Spink (13) has the following to say about the construction of the sharp, square edged, thin-plate concentric orifice plate:

> "In order to use the published coefficient data within the standard tolerances, the orifice should be made to the following specifications in which d_o = orifice diameter and d = pipe diameter:
>
> 1. The thickness should not exceed any of the following limits: $d_o/8$, $d/50$, or $(d-d_o)/8$, in the cylindrical portion. If a thicker plate is required for rigidity, the outlet face may be beveled or recessed to attain the desired dimension. This should be done in such a way that a straightedge laid across the bevel or recess will form an angle not less than 45° to the axis of the pipe.
> 2. The upstream edge should be square and as sharp as possible. Any rounding should not exceed 0.025% of the diameter of the orifice to assure measurement within 0.1%.
> 3. The upstream face should be at least as smooth as good commercial rolled stock.
> 4. The portion of the plate which extends inside the pipe should be flat within 0.01 in. per in. of radius.
> 5. The orifice plate should be centered in the pipe in such a way that the eccentricity is less than 3% of the pipe diameter."

The orifice plate should be equipped with a tang plate as shown in Figure 2. This plate can be stamped with the proper diameter ratio and line size and it also serves as a handle during installation and removal. The orifice plate should also be equipped with a weep hole (1). For air flow measurement, the weep hole should be located on the bottom of the orifice plate; this allows any condensation which forms in the line to pass the plate. The exact weep hole location and the recommended weep hole sizes are shown in Figure 2. Normally, the error involved in flow measurements due to the installation of a weep hole is extremely small; however, for small orifice bores it may be desirable to correct the flow measurement accordingly. Weep holes should not be used when the orifice bore is 7/8 in. or smaller.

Care should be taken so that the orifice plate flanges are welded perpendicular to the pipe. Nipples for vena contracta pressure taps and static pressure taps should be welded on the outside of the pipe. They should be perpendicular to the pipe wall, and should be at least 2½ times the diameter of the tap hole in length (see Figure 1). The diameter of the tap holes may vary but should probably not be smaller than 1/8 in. nor larger than 1/4 in. The tap hole should be drilled with the nipple as a guide after it has been welded in place. Any burrs around the hole on the inside of the pipe should be removed so that the surface is smooth.

As far as static pressure taps are concerned, they are usually located upstream of the orifice plate. Some

Figure 1. Pipe Wall Pressure Taps

(After Spink)

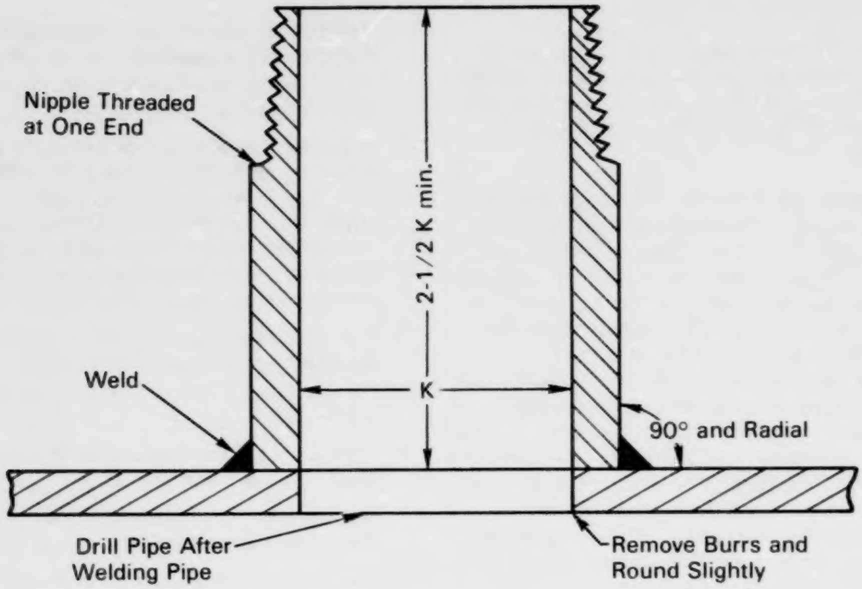
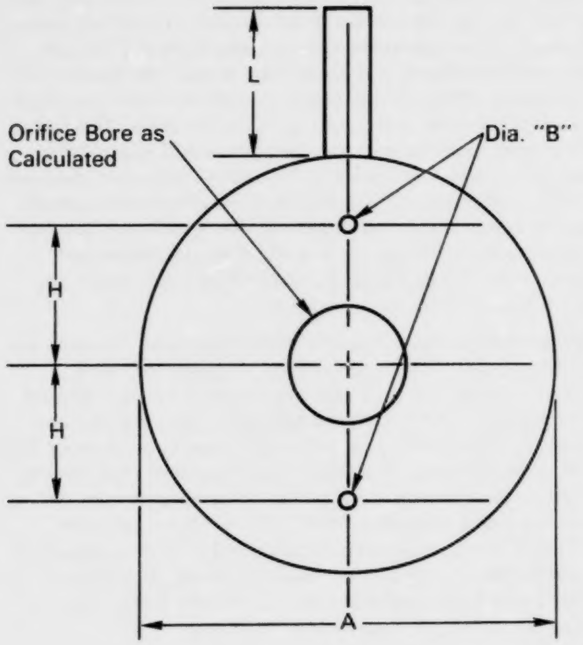

Nipple Threaded at One End
2-1/2 K min.
K
Weld
90° and Radial
Drill Pipe After Welding Pipe
Remove Burrs and Round Slightly

Figure 2. Orifice Plate Dimensions

(After Cusick)


L
Orifice Bore as Calculated
Dia. "B"
H
H
A

A = diameter of flange bolt circle minus diameter of flange bolt hole

L = at least 3 in. for pipe sizes up to 3 in.

L = at least 5 in. for pipe sizes above 3-1/2 in.

H = inside diameter of pipe minus "B"/2

Pipe Size (in.)	Dia. "B" (in.)
1-1/2 to 4	1/16
5 & 6	3/32
8, 10 & 12	1/8
14 to 20	5/32

Drill only one hole: upper for liquid flow, lower for gas or vapor flow. If the orifice bore is 7/8 in. or smaller, do not drill Hole "B".

expansion factor expressions, however, are in terms of a downstream static pressure or an average upstream and downstream static pressure. It is important that the static pressure tap(s) be located in accordance with the expansion factor expression. In the case of upstream static pressure taps, it may be advisable to locate the tap at least 4 pipe diameters upstream of the orifice plate to avoid the compression region in front of the plate. It should not be located so far in front of the plate, however, that there are significant pipe losses between the tap and the plate.

The orifice plate should be installed with two rubber gaskets, and care should be taken to center the orifice plate as much as possible. The bolts around the plate should be tightened up evenly.

Selection of a Secondary Flow Element(s)

To help in the proper selection of a secondary flow element, the reader is referred to the section of this paper on "Secondary Flow Elements." Certain basic guidelines should be followed, however. First of all, a secondary meter should always be sized to measure the actual differential, not the high and low pressures separately followed by an arithmetic subtraction. The latter method usually results in a substantial loss of accuracy.

Furthermore, it may often be appropriate to have several secondary elements with different differential pressure ranges available for use with a given primary element, since it is often the readability of the secondary element that limits the flow range of the overall gas flow measurement system.

Finally, it is usually appropriate with manometers to use either water or mercury for manometer fluid when making field differential pressure measurements. Fluids which have a specific gravity close but not equal to that of water should be avoided, since the possibility of contamination with water always exists in the field. Mercury, on the other hand, has a specific gravity that is so much different from that of water, that minor contamination with water does not affect the readings significantly.

Connections Between Primary and Secondary Flow Elements

A number of factors should be considered when making differential pressure connections between primary and secondary flow elements In particular:

1. The manometer should be located in a shaded area as close to the primary element as possible.
2. For gas flow, the manometer should be mounted above the air line so that the tubing slopes at least 1 in. per ft. This allows any condensation in the tubing to roll back into the air line.
3. For distances less than 50 ft, the tubing between the primary and secondary elements should be at least 3/16 in. inside diameter. For distances greater than 50 ft, the diameter should be increased by approximately 1/8 in. for each additional 50 ft.
4. For greatest accuracy, the tubing on both sides of the head meter should be of the same length and should be in the same thermal environment.

The tubing used to connect the primary and secondary flow elements may be made of a number of commercially available materials. Copper tubing or pipe is recommended for permanent installations. Materials such as rubber, polyethylene and vinyl are fine for less permanent installations and are more amenable to changes in the way the system is connected. Special care should be exercised when using hard-walled tubing such as polyethylene in an outside environment, since low temperatures may cause the tubing to crack.

Valves should be placed on both primary and secondary elements. Needle valves and other restrictive valves such as plug and globe valves are not recommended. "Equalizer" arrangements and check valves should be used on manometers where a possibility of excessive differential pressure exists. The equalizer is merely a cross-connection between the high and low pressure sides of the manometer with a manual shut-off valve. Lever-operated, 1/4-turn gas valves are very convenient for this application. Generally speaking, the equalizer valve is left open unless a reading is taken. This protects the fluid in the manometer against over-pressure. Some manometers, such as those for measuring line pressure, may not require either equalizers or check valves since the possibility of over-pressure may not exist.

A number of tubing connectors are available. For permanent installations, double-ferruled connectors are recommended. These can be used on any hard-walled tubing and can be used on soft-walled tubing with the proper tubing insert. Fittings can be bought which will adapt to various types and diameters of tubing and pipe. pipe. For less permanent installations, barbed tube or hose connectors are recommended for use with flexible tubing such as rubber or vinyl. This type of fitting makes connecting and disconnecting tubing quick and easy and yet assures a positive seal. They are limited to low pressure installations, however.

Troubleshooting the Flow Measurement System

After a gas flow measurement system is designed and built, it is essential that it be checked out in detail before it can be used with confidence. First, care should be taken to ensure that the system has been built according to specifications. This should be followed by a thorough leak test. The primary element, secondary element, pressure taps, tubing connections, valves and other related items should all be checked with a soap solution when the line is under relatively high pressure. In the case of differential pressure flow systems, after the leak test has been performed, the response of the manometers should be checked. A manometer should respond rapidly to the pressure differential it measures. It if does not, this is a sign of a possible leak. An additional leak check should be performed by closing the valves on the primary element when there is a differential being measured. If the manometer reading changes significantly or drifts gradually, this may be a sign of a leak in the tubing or fittings between the manometer and the primary element valves. An increasing differential may indicate a leak to atmosphere in the low pressure tubing; a decreasing differential may indicate a leak to atmosphere in the high pressure tubing. A decreasing differential may also be a sign of a leak in the equalizer valve between the high and low pressure sides of the manometer.

It is also important to make sure that the differential pressure manometers zero properly. This should always be done with the system under pressure. It is also important to make sure that the blower "sees" the primary element during this check; in other words, no valves should be closed between the blower and the primary element. This is necessary since if pulsation is a problem, the manometer zero may be significantly affected (see the "Pulsation" section of this paper).

An additional check should be made to see if pulsation exists in the system. The manometer differentials should be read with two different lengths of connecting tubing, ranging from several feet to 15 ft or more in length. If the differential varies with tubing length then this is probably a sign of pulsation (see the "Pulsation" section of this paper).

Look for manometer fluid oscillations at different flow rates and line pressures. Significant oscillations can have an adverse effect on air flow measurement. These oscillations are due to unsteady conditions in the line caused by inadequate lengths of straight pipe, aeration tank water level variations, or possibly to blower pulsation, although it is not to be inferred from this that blower pulsation is always accompanied by manometer fluid oscillations (see the "Pulsation" section of this paper). If there is an oscillation problem and it is related to inadequate lengths of straight pipe, then straightening vanes (1) (13) or a new orifice plate location may help. If it is related to aeration tank water level variations, then this can be at least partially overcome by introducing a headloss such as a throttled valve between the primary element and the air release point in the aeration tank. If the oscillation problem is related to pulsation, then it can usually be overcome by one of the methods listed in the "Pulsation" section of this paper.

A check should also be made to ensure that as the air flow is steadily increased throught the line, the corresponding differential pressure increase is steady as well. A negative differential pressure reading is a sure sign of a problem.

It is an excellent idea to check the flow measurement system against as many known flow rates as possible. It should be kept in mind that agreement at just one flow does not guarantee agreement at other flows and line pressures. Positive displacement blowers can provide a very good check on an air measurement system as long as an accurate blower air flow computation is performed (the information necessary to perform this computation is usually available from the manufacturer). Indirect inferences as to flow rate can also be used to check the flow measurement. These include oxygen transfer measurements and diffuser headlosses.

In summary, after a differential pressure flow measurement system has been constructed, the following checks should be performed as a minimum requirement:

1. Make sure that the system has been built according to specifications.
2. Perform a thorough leak test.
3. Check the response of the manometers to changes in differential pressure.
4. Check the manometer zero under pressure.
5. Check the differential pressure readings with several different lengths of connecting tubing.
6. Check for manometer fluid oscillations.
7. Make sure that the differential pressure increase is steady with a steady increase in flow.
8. Check the flow measurement system against as many known flow rates as possible.

Pulsation

Pulsation, as used here, refers to the high frequency pressure oscillations that are produced by positive displacement blowers, reciprocating compressors, and other devices which create dynamic disturbances. It is a well known fact that pulsation can be a major source of error with head meters. The pressure oscillations at the two pressure taps of the head meter do not necessarily cancel out, with the result that the differential produced may be considerably different than that which would have been produced under steady flow conditions. Different types of head meters are affected differently by this problem, but it is probably fair to say that all are affected to some extent. The purpose of this section of the paper is to relate some recent personal experience with pulsation. Symptoms of the problem will be discussed as well as methods of correcting it.

Pulsation was experienced recently during clean water tests conducted by the Los Angeles County Sanitation Districts at the Joint Water Pollution Control Plant in Carson, California. The tests are part of an aeration equipment evaluation sponsored by the EPA. It would be appropriate at this point to describe the basic layout of the test system.

The clean water test facility in Carson, California employs a 74,800 gal aeration tank, 20 ft × 20 ft × 25 ft SWD in dimension, It utilizes a rotary lobe positive displacement blower for air delivery which has a maximum capacity of approximately 900 scfm at 12 psig differential and 1750 rpm. For the present work underway at the Districts' facility, the blower has been sheaved down until its output is approximately 420 scfm. Different air rates to the aeration tank are obtained by wasting excess air through a waste valve upstream of the flow measurement system. The flow measurement system consists of two different size air lines, depending on the flow rates to be measured. Flow readings are obtained with both an orifice plate and an Annubar (a modification of the pitot-static tube) in each air line. The blower has 6 in. suction and discharge piping (including a discharge silencer) which continues for approximately 20 ft until the start of the flow measurement piping. At this point there is a line size reduction to 3 in. for the smaller air line and to 4 in. for the larger air line. These pipe sizes continue for approximately 6 ft to the orifice plate in each line. Several feet after the orifice plate in each line, the pipe size is further reduced by one pipe size to accommodate the Annubars. Approximately 5-1/2 ft separates the orifice plates and the Annubars. After the Annubars, the pipe expands back to 6 in. to go through the aeration tank wall. Approximately 8-1/2 ft separates the Annubars and the aeration tank wall. Inside the aeration tank there is approximately 15 ft of 6 in. pipe and 130 ft of 4 in. fine bubble diffuser header pipe.

With this information as a background, it will be possible to discuss the pulsation phenomena that were observed. Prior to the start of the study, the air flow measurement system was checked out thoroughly. Since the aeration tank was empty at the time, a throttled valve was used to simulate various water depths at different air flow rates. Under these conditions the orifice plates and the Annubars agreed very well and there were no signs of any problems.

After the tank was filled with water, however, it was clear that the agreement was no longer good. What is more, certain phenomena were observed which were really difficult to explain. For one thing, at low air flow rates through a given air line, the Annubar manometer read negative instead of positive. Extensive leak checks were performed to no avail, and a conclusion was reached that there was a pressure disturbance of some type in the line. What was even more baffling, however, was the fact that by changing the lengths of the manometer tubing, particularly on the Annubar, considerably different differential pressure readings could be produced. Short tubing lengths (approximately 3 ft) tended to produce differential pressure readings that differed by as much as 2 in. H_2O from those produced by longer tubing lengths (approximately 12-13 ft). Again, extensive leak checks were performed to no avail. After talking to a number of experts in the field, it was felt that larger tubing diameters might help (1/4 in. i.d., instead of 5/32 in. i.d.) as well as making the tubing exactly the same length on both sides of the manometer. After making these corrections, the problem was still experienced with the Annubar but not with the orifice plate. A decision was made at that time to disregard the readings from the Annubar as it was obviously being affected by some type of pressure disturbance in the line. It was located upstream from a check valve and other fittings, and it was felt that in some way they might be interferring with the readings. It was not known at that time that the cause of the problem was pulsation.

A large number of clean water tests were conducted with the orifice plates as the means of flow measurement. The Annubar readings were taken but were not used for data analysis. Since the results of the tests did not meet the manufacturer's expectations, a decision was made to check out the air measurement system in more detail. At this time, while operating in the larger of the two air lines, a valve was closed downstream of the Annubar such that the blower was wasting entirely through the waste valve. Under these conditions, the Annubar manometer should have read zero since there was no net flow past it. Instead, it registered approximately 0.5 in. H_2O in a negative direction with short tubing leads and approximately 2.5 in. H_2O in a negative direction with long tubing leads. It was at this time that pulsation was first suspected. A valve upstream of the flow meter was shut off, and with this the Annubar manometer went to zero. This essentially confirmed the pulsation phenomena.

A decision was made soon afterwards to install a large reservoir just past the discharge silencer of the blower in an attempt to help dampen the pulsation. This is one of the recommended procedures to help eliminate pulsation when it exists. The tank is cylindrical, 2-1/2 ft in diameter and 7-1/4 ft high, with a capacity of approximately 266 gal (35.6 ft^3). The air inlet is side mounted near the top, and the outlet is bottom mounted. Since the tank has been installed, essentially perfect agreement has been obtained between the Annubars and the orifice plates over the full range of flows and pressures. Furthermore, the manometers now zero perfectly and the manometer readings are not affected by short and long tubing leads. Also, the Annubar manometers do not read negative at low air flows as they did prior to the installation of the reservoir.

Although the exact amount of error is not known at this time, it does appear that the effect of pulsation on air flow measurement was major at the combination of low air rates (approximately 80 scfm) and high water depths (25 ft). This was determined by a comparison of all the orifice plate and Annubar data that had been collected to date. There was a definite correlation of the agreement between the Annubars and orifice plates with the air flow and water depth. It appears that the data collected at high air rates (approximately 400 scfm) and low water depths (10 ft) was very nearly correct; however, as the air rate decreased and the water level increased, the error became much worse. It is interesting to note that based on these findings, it would appear that the pulsation problem was worse at low line velocities.

The most significant outcome of the experience with pulsation was that some of the symptoms were recognized. At this point, it would appear that pulsation may be accompanied by the following phenomena:

1. The manometer will not zero under a "no-flow" condition with the blower operating in the line.
2. The manometer readings are affected by the length of the connecting tubing.
3. The manometer reads negative under a positive flow condition.

It is important to point out that the absence of these symptoms does not necessarily mean that the pulsation problem does not exist. If one or more symptoms are discovered, however, pulsation may be present.

As far as corrective measures are concerned, Spink (13) has the following to say:

> "The practical approach is to reduce or eliminate the pulsation by installing added capacity or volume together with added pressure drop in the line between the meter and the source of pulsation."

He also goes on to say:

> "Measurement may be improved by one or a combination of the following:
>
> 1. Operating at a higher differential; i.e., in a multiple meter run installation, shutting off one or more runs.
> 2. Installing a higher range differential gauge and changing operating conditions in order to use the increased range with the existing orifice, as by reducing the flowing pressure, etc.
> 3. Reducing the pipe run diameter so as to use a higher orifice-to-pipe-diameter ratio, still operating at differentials as high as practicable. Increasing the d_o/d ratio will reduce the pulsation error if the operating differential remains constant.
> 4. Installing mufflers, headers, restrictions, or combinations of capacity and pressure drop between the primary device and the source of pulsation to reduce pulsation amplitude.
> 5. Locating the primary device at a point where the pulsation amplitude is lower (as on the suction side of compressors)."

In our case, the installation of the reservoir (added capacity) between the blower and primary device corrected the problem.

As of yet, there are no design criteria that will guarantee that pulsation will not be a problem in a gas flow measurement system. Consideration of the factors above, however, should help in the development of a good design. Primary emphasis should be on providing substantial pipe volume and headloss between the blower and primary device. Once built, each system should be checked for the symptoms mentioned earlier.

Conversion of Standard Volumetric Flow Rates of Air to Mass Flow Rates of Oxygen.

One method of converting a volumetric air flow rate in scfm to a mass oxygen flow rate in lb/hr is to first calculate the fraction of the scfm flow that is dry air. This can be done by multiplying by the mole fraction of dry air at standard conditions. This mole fraction is related to partial pressure in the following manner:

$$Y_{das} = (14.70 - p_{ws})/14.70$$

in which:

Y_{das} = mole fraction of dry air at standard conditions of 68°F, 14.70 psia, and 36% relative humidity

p_{ws} = partial pressure of water vapor at standard conditions of 68°F, 14.70 psia, and 36% relative humidity, psia

Making this substitution, $Y_{das} = 0.9917$. Thus, the flow rate of dry air at STP becomes:

$$Q_{das} = 0.9917\,Q_a \tag{21}$$

in which:

Q_{das} = dry air portion of Q_a, cfm

Q_a = air flow at standard conditions of 68°F, 14.70 psia, and 36% relative humidity, scfm

The density of dry air at standard temperature and pressure is 0.07521 lb/ft^3 (6). Thus, the weight flow of dry air in lb/hr is given by:

$$w_{da} = (0.07521)(60)\,Q_{das} = 4.513\,Q_{das} \tag{22}$$

in which:

w_{da} = weight flow of dry air, lb/hr

Combining Equations 21 and 22 yields:

$$w_{da} = 4.475\,Q_a \tag{23}$$

Dry air is variously taken as 23.0 to 23.2% oxygen by weight (6). Using 23.1% as an average, the weight flow of oxygen is related to the standard volumetric flow of air in the following manner:

$$w_{O_2} = (4.475)(0.231)\,Q_a = 1.034\,Q_a \tag{24}$$

in which:

w_{O_2} = weight flow of oxygen, lb/hr

Conversion of Volumetric Flow Rates from Standard to Actual Conditions

It is often necessary to determine the actual cfm flow of air, particularly when correlations are being made with headloss. From ideal gas considerations, the scfm and cfm air flows are related by temperature, pressure, and relative humidity in the following manner:

$$(25) \quad Q = (14.70/p_f)(T_f/528)[(p_b - p_{bw})/p_b][p_f/(p_f - p_w)]$$

in which:

Q = gas flow, cfm

For air, using standard base conditions of 68°F, 14.70 psia, and 36% relative humidity, the expression reduces to:

$$(26) \quad Q = 0.0276\, T_f\, Q_a/(p_f - p_w)$$

Power Measurement

Accurate determinations of both gas and turbine horsepower are essential in the field of oxygen transfer. Basically, they are needed for aeration system design, economic comparisons, and data correlations. This section of the paper will discuss the meaning of various horsepower terms used in the oxygen transfer field and will attempt to point out where confusion exists. It will also discuss various methods commonly used to make power determinations, as well as the limitations of certain approaches. Furthermore, particular emphasis will be placed on the need to standardize the way in which clean water horsepower data are reported for advertising or comparison purposes.

General Definition of Terms

At this point it would be appropriate to present the following general definitions of some of the basic horsepower terms used in oxygen transfer testing. These definitions will be elaborated on in later sections of the paper.

1. gas horsepower — that part of the total aeration system power that is specifically related to the gas supply
2. turbine horsepower — that part of the total aeration system power that is specifically related to the operation of a mechanical device such as a turbine or pump
3. water horsepower — the work performed on the water in the form of either gas or turbine energy
4. total water horsepower — the sum of both the gas and turbine water horsepower
5. delivered horsepower — in the case of gas horsepower, the theoretical horsepower required at the blower to deliver a given mass flow of gas through a diffuser system operating under a given static head; in the case of turbine horsepower, the horsepower required at the output shaft of the gear box to turn a shaft and impeller at a given rpm in water, under actual conditions.
6. total delivered horsepower — the sum of both the gas and turbine delivered horsepower
7. brake (shaft) horsepower — the horsepower developed or required at the shaft of a rotating piece of equipment
8. total brake horsepower — the sum of both the gas and turbine brake horsepower
9. wire horsepower — the electrical horsepower drawn by a motor
10. total wire horsepower — the sum of both the gas and turbine wire horsepower

The Need for Standardization

When reviewing clean water test data, it is often unclear as to how reported power determinations were actually made. Confusion may exist as to the following:

—Were the power determinations reported as delivered, brake, or wire horsepower?

—Were the power determinations measured directly or calculated?

—If calculated, what calculation procedure was used (adiabatic, polytropic, etc.)?

—Were actual, standard, or some other ambient conditions used?

—Was the effect of line loss, as well as diffuser loss included?

—Did the determinations include a blower suction loss?

—At what gas temperature was the diffuser headloss measured?

—At what water temperature was the turbine power draw determined?

—What blower, motor, and drive efficiencies were used?

Questions such as the above should not exist about any reported horsepower data.

It would seem that in certain cases, rather than just answering questions like the above in the context of a report, it would be desirable to report standard horsepower data. In other words, standard conditions and procedures should be established for power measurements such that when reference is made to standard delivered, brake, or wire horsepower, there is no confusion as to what is meant. This would apply primarily to cases where the results of shop or field tests are to be reported for advertising or comparison purposes. In the case of shop and field performance tests, where power determinations are to be made for equipment acceptance under specified design conditions, the reporting of standard horsepower data alone would certainly not be appropriate. In this case, the horsepower results should be reported in terms of specified design condtions; however, the additional reporting of standard horsepower data would facilitate the comparison of various shop and field performance tests made around the country.

It would seem, therefore, that it would be appropriate to define a set of standard conditions and procedures for power determinations. It is not a purpose of the power section of this paper to determine the standard conditions and procedures to be used, but rather to show a need for standardization where it exists. Any standard conditions or procedures should be decided upon by representatives from the oxygen transfer field. However, possible standards will be suggested where appropriate in this paper.

In the case where reporting standard horsepower data is appropriate, there are some arguments for reporting only standard delivered horsepower instead of standard delivered, brake, and wire horsepower. For one thing, it would not be necessary to define standard motor, blower, gear box, and coupling efficiencies. It is the author's opinion, however, that standard brake and wire horsepower should also be reported. Comparisons made between two component aeration systems would then be more realistic. Due to the different efficiencies associated with a blower compared to a turbine, reporting only standard delivered horsepower would be very misleading as far as the overall power requirements of the aeration system are concerned.

It is important to point out that standard clean water horsepower numbers should not be used directly when going from clean water test results to aeration system design. When applying clean water test data to design, it is important that horsepower determinations be made which use actual ambient and operating conditions and horsepower formulations which apply directly to the blower being used. The purpose of standard horsepower determinations is only to make an operating cost comparison of various types of diffuser systems much easier.

Gas Horsepower

Gas horsepower is that part of the aeration system power requirement that is specifically related to the gas supply. It refers to the general class of gas horsepower determinations, including water, delivered, brake, and wire horsepower (see Figure 4). In many cases, shop and field performance tests are run for equipment acceptance and the actual blower - motor combination is used. In these cases, it is appropriate to make direct power measurements. In cases where gas horsepower determinations must be made without reference to a specific blower or motor, it is more appropriate to calculate the power. Most often this calculation is based on the adiabatic compression formula.

Adiabatic Compression Formula

The adiabatic formula can be derived from basic thermodynamic principles. Fair and Geyer (3) have shown the derivation. It is important to note, however, that the final form of the equation in their derivation makes some assumptions as to ambient conditions. Basically, the formula represents the work done along an adiabatic path between two gas states. It is based on the ideal gas equation, $pV = nRT$, and the adiabatic relationship, pV^{C_p/C_v} = constant, where C_p and C_v are the specific heats at constant pressure and volume, respectively. The work performed is calculated by integrating the pressure-volume work between the two states:

$$\text{adiabatic work} = \int_{p_1,\,T_1}^{p_2,\,T_2} p\,dV \tag{27}$$

After integrating, the following general form of the adiabatic formula can be obtained:

$$\begin{array}{c}\text{adiabatic}\\ \text{wire}\\ \text{horsepower}\end{array} = [wRT_1/(550\ Ke_be_de_m)][(p_2/p_1)^K-1] \tag{28}$$

in which:

w = weight flow of gas, lb/sec

R = gas constant, ft·lb_f/lb_m·°R (53.5 for air)

T_1 = absolute inlet temperature, °R

p_1 = absolute inlet pressure, psia

p_2 = absolute outlet pressure, psia

K = $(k-1)/k$ (0.283 for air)

k = ratio of specific heats, C_p/C_v (1.395 for air)

e_b = blower efficiency, decimal %

e_d = drive efficiency, decimal %

e_m = motor efficiency, decimal %

The basic form of this equation can be found in Reference (10). The case where the motor efficiency, e_m, is equal to 1 is equivalent to the motor brake horsepower. Furthermore, the case where the motor efficiency, e_m, and the drive efficiency, e_d, are both equal to 1 is equivalent to the blower brake horsepower. Finally, the case where all the efficiencies are equal to 1 refers to the perfect adiabatic case where there are no losses.

Substituting the known quantities for air into Equation 28 gives:

(29)

$$\begin{array}{l}\text{adiabatic} \\ \text{air wire} \\ \text{horsepower}\end{array} = [(5.729\text{x}10^{-3})\gamma_{air}\ QT_1/(e_b e_d e_m)][(p_2/p_1)^{0.283}-1]$$

in which:

γ_{air} = air density at the temperature, pressure, and relative humidity for which Q is reported, lb/ft^3

Q = gas flow, cfm

It is important to note the limitations of the adiabatic compression formula. Too often it is used with the idea that it accurately describes the operation of all blowers. While many blowers are nearly adiabatic, there are some that may be closer to polytropic in operation (11). The polytropic horsepower formula for air is similar to the adiabatic horsepower formula for air (Equation 29) except that the value of 0.283 is replaced by 0.283/e_{poly}, where e_{poly} is the polytropic efficiency of the blower (a purely thermodynamic property). Furthermore, the value of e_b in the adiabatic air horsepower formula is replaced by the mechanicl efficiency of the blower. Both the polytropic and adiabatic formulas have been used to describe the operation of centrifugal blowers. The operation of positive displacement blowers, on the other hand, has usually been described with the adiabatic formula. With this in mind, it is probably reasonable to use the adiabatic formula when reporting clean water data, since it has been used to describe the operation of both types of blowers. Furthermore, the determination of the polytropic and mechanical efficiencies of a blower is usually not easy. It should be kept in mind, however, that comparisons made between various aeration systems on the basis of adiabatic power may not hold true when the comparison is made on the basis of polytropic power. Also, comparisons made between the horsepower requirements of a given aeration system at various water depths may differ when polytropic power is used instead of adiabatic power.

It should also be kept in mind that the adiabatic formula (Equation 28 with all efficiencies = 1) should not generally be used to calculate the relative internal energy level of the gas at a particular point in the gas distribution piping. The relative internal energy level, as used here, refers to the increase in internal energy above that at ambient conditions. From the First Law of Thermodynamics, the increase in internal energy of a gas is the difference between the work performed on it and any heat loss which may occur. For an adiabatic process, where the heat loss is zero, the increase in internal energy of a gas is thus equal to the work performed on it. The process of supplying air to an aeration tank, however, is not perfectly adiabatic in nature. This is to say, the blower is not perfectly efficient, adiabatically speaking, and the pressure and thermal losses in the distribution piping are not usually adiabatic. This means that the adiabatic formula cannot be used to give the relative internal energy level of the gas. It is important to note, however, that the adiabatic formula can be used at any point in the gas distribution piping to determine the adiabatic work that would be required to compress the gas to the line pressure at that point. This is often a very meaningful calculation.

Furthermore, the adiabatic formula (Equation 28 with all efficiencies = 1) cannot be used to calculate the work that is actually performed in the water. The actual water horsepower due to the gas is the pressure-volume work that is done as the gas rises and expands throughout the depth of the aeration tank. This work is probably much closer to being isothermal in nature since the expansion probably takes place at a temperature close to that of the water due to the very high heat capacity of water relative to gas. If there were no oxygen transfer in the tank, it would be theoretically possible to calculate the work performed in the water with the following integrations:

(30)

$$\text{isothermal work} = \int_{\text{gas release point}}^{\text{water surface}} p\,dV$$

Using the ideal gas law and assuming the temperature is constant:

(31)

$$\text{isothermal work} = nRT \int_{\text{gas release point}}^{\text{water surface}} dV/V$$

$$= nRT \ln (p_{inlet}/p_{surface})$$

in which:

n = number of moles of gas

p_{inlet} = absolute static head at the gas release point

$p_{surface}$ = absolute pressure at the aeration tank water surface

Since under dynamic conditions oxygen transfer does take place, performing this integration exactly is not possible. It would seem, however, that Equation 31 could be used to provide an approximation to the maximum gas work performed in the water at a time of zero oxygen transfer. Knowing the actual percent transfer, it is possible to place a lower limit on the actual gas work performed in the water (based on mass considerations). Thus, Equation 31 might be used to describe an approximate range for the actual gas work performed in the water.

Water horsepower is really only significant from a theoretical standpoint. It may be of some value in data correlations, particularly in regard to mixing. Generally speaking, however, what is most important is the power required to deliver a given quantity of gas through a diffuser system under specified conditions. As mentioned previously, this can usually be approximated by the adiabatic compression formula.

Gas Delivered Horsepower

Gas delivered horsepower is usually considered to be the theoretical adiabatic horsepower (Equation 28 with all efficiencies = 1) required at the blower to supply gas through a diffuser system operating under a given static head. Thus, for the special case of air,

$$(32)\quad \begin{matrix}\text{air}\\ \text{delivered}\\ \text{horsepower}\end{matrix} = (5.729\text{x}10^{-3})\ \gamma_{air} Q T_1[(p_2/p_1)^{0.283} - 1]$$

After conversations with a number of people in the oxygen transfer field, it seems there are a number of different opinions on the way in which this formula should be applied. First of all, many experts consider the delivered horsepower to be the adiabatic power required at the inlet to the diffuser, rather than at the blower. In other words, there is some confusion as to whether line losses should be included in the gas delivered horsepower calculation.

It should be considered at this point that the adiabatic formula (Equation 28) provides a closer approximation to the actual power required when applied at the blower. For this reason, it would seem to the author that the best definition of gas delivered horsepower would be the theoretical adiabatic power required at the blower. In most cases, the horsepower so calculated would be more realistic and practical.

The gas delivered horsepower formula (Equation 28 with all efficiencies = 1) can be used with actual ambient and operating conditions to approximate the gas delivered horsepower requirement on a given day at a given installation. This is sometimes necessary from the standpoint of aeration system design and plant operation. When reporting clean water test data for advertising or comparison purposes, however, the use of actual ambient and operating conditions to calculate gas delivered horsepower can be very misleading. It is clear from the gas delivered horsepower formula (Equation 28 with all efficiencies = 1) that it is possible to obtain a wide range of delivered horsepowers for a given mass flow of gas and gauge discharge pressure. This is because the ambient conditions are also important in determining the horsepower requirement. This fact may tend to cloud the real issue when comparisons are made between various clean water tests across the country. What is really important from a gas standpoint is the gas flow and gauge discharge pressure at which a given test was conducted. Different ambient conditions for the same gas flow and gauge discharge pressure can vary the power requirements by over 30%. In the case of power data to be used for advertising and comparison purposes, calculation of a standard gas delivered horsepower could be made from the gas flow and discharge pressure by assuming certain standard conditions.

Also, the use of actual system line losses could very well tend to confuse diffuser horsepower comparisons made across the country. Since line losses for any diffuser system are usually easily controlled by varying piping size, it would be desirable to assume a standard line loss when calculating the standard gas delivered horsepower. Furthermore, if possible, diffuser headloss readings should be corrected to a standard gas temperature before using them to calculate standard gas delivered horsepower.

There is a need, therefore, to standardize the adiabatic power measurements when reporting clean water test results for advertising or comparison purposes. One proposed definition of standard air delivered horsepower might be that power calculated from the adiabatic compression formula (Equations 32 or 29 with all efficiencies = 1) when compressing an scfm flow rate of air at standard ambient conditions of 68°F, 14.70 psia, and 36% relative humidity to a discharge pressure set by the static head, an 0.8 psi line loss and a diffuser headloss corrected to 68°F. Furthermore, a 0.1 psi suction loss would be assumed for the blower.

The standard ambient conditions mentioned are of course the present standards for air flow measurement. The value of 0.8 psi is considered to be a reasonable average system line loss, but certainly many other values could be used. The standard temperature of 68°F was selected for the diffuser headloss correction because it is felt that in most cases the diffuser air temperature is near that of the water, and the standard water temperature for oxygen transfer measurements is 68°F. Finally, the blower suction loss of 0.1 psi is felt to be fairly representative.

Making these substitutions into the air delivered horsepower formula (Equation 32), the following expression for standard air delivered horsepower is obtained:

$$(33)\quad \begin{matrix}\text{standard air}\\ \text{delivered}\\ \text{horsepower}\end{matrix} = 0.227\ Q_a \left\{[(15.5+h_L+H)/14.6]^{0.283} - 1\right\}$$

in which:

h_L = diffuser headloss corrected to 68°F, psi

H = static head, psig

While it is realized that this is not an actual power determination, and is definitely not to be used for design work, it does make comparisons of clean water test data collected on different days in different parts of the country more meaningful.

At this point, it would be appropriate to discuss the three measurements, Q_a, H, and h_L, that are required for the determination of standard air delivered horsepower from Equation 33. The standard air flow, Q_a, has been discussed earlier in this paper. The static head determination can be made most accurately with the use of a bubbler device (see Figure 3). The bubbler is essentially a vertical pipe with an outlet at the diffuser level. In practice, a small air compressor can be used to bubble air down through the bubbler pipe to the diffuser level. The bubbler piping should be large enough so that virtually no friction losses are obtained. A connection is made between a pressure tap in the bubbler piping and one leg of a mercury manometer, so that static pressure can be read directly. A bubbler is an accurate way of measuring the static head because it compensates for the gas hold-up above the diffusers.

Diffuser headloss can be determined by reading diffuser inlet pressure against the static head with a manometer. In order to sense diffuser inlet pressure, a pressure tap should be installed at the inlet to a diffuser(s). A connection should be made to one side of a manometer, with the bubbler static pressure on the other side. The resulting differential will be the diffuser headloss. To accompany the headloss reading, measurements should also be taken of the air flow, diffuser air temperature, and static head. As mentioned previously, for standard horsepower determinations, the diffuser headloss should be corrected to a standard diffuser air temperature of 68°F.

Gas Brake Horsepower

Brake or shaft horsepower refers to the power associated with the rotating shafts of mechanical equipment. It can refer to either input or output horsepower, depending on the device. With motors, for example, the brake horsepower developed is the motor output horsepower. With blowers, the brake horsepower required is the blower input horsepower. In an actual application where the motor is connected to the blower by means of a flexible coupling, belt drive, or gear reducer, there are power losses between the motor and the blower. This means that the motor brake horsepower developed is greater than the blower brake horsepower required by the amount of loss. It is important, therefore, when talking about gas brake horsepower to distinguish between the motor and blower brake horsepower (see Figure 4).

In the case where the actual blower - motor combinations are being tested for equipment acceptance, it is appropriate to make direct power measurements with the techniques discussed in the section of this paper on "Shaft Horsepower Determinations". When estimates of brake horsepower are required without reference to a particular blower - motor combination, then it is usually calculated from the theoretical adiabatic horsepower by using the appropriate efficiency.

The blower brake horsepower is related to the theoretical adiabatic horsepower by the blower efficiency, e_b:

$$(34)\quad \text{blower brake horsepower} = \text{theoretical adiabatic horsepower}/e_b$$

in which e_b is in decimal %.

The blower efficiency depends on a number of factors, including blower type (centrifugal or positive displacement), number of stages, and load (including speed, pressure differential, and ambient conditions). A range of blower efficiencies applicable throughout the field today might be 0.50-0.80. Where power measurements are made directly and theoretical adiabatic horsepower is calculated from the adiabatic formula (Equation 28 with all efficiencies = 1), the blower efficiency can be determined from Equation 34.

Motor brake horsepower is related to the blower brake horsepower by the drive or coupling efficiency, e_d:

$$(35)\quad \text{gas motor brake horsepower} = \text{blower brake horsepower}/e_d$$

in which e_d is in decimal %.

This efficiency varies with the type of coupling or drive and with load. Coupling or drive efficiencies are usually pretty high, probably between 0.9 and 1.0. If brake horsepower measurements are made directly for the blower and motor, then the efficiency, e_d can be determined from Equation 35.

When reporting clean water test data for advertising or comparison purposes, it would be appropriate to use a standard gas brake horsepower. It is the author's opinion that standard gas brake horsepower should be related to standard gas delivered horsepower by a standard blower efficiency, e_{bs}, and a standard drive efficiency, e_{ds}. Reasonable values for these standard efficiencies might be 0.70 and 0.95, respectively, although many others could be proposed.

Gas Wire Horsepower

Wire horsepower is the horsepower drawn by the motor. In the case where the actual blower-motor combinations are being tested for equipment acceptance, it is appropriate to make these power measurements directly (see the section of this paper on "Turbine Wire Horsepower"). When estimates of wire horsepower are required without reference to a particular blower-motor combination, then it is usually calculated from the brake horsepower by using the appropriate efficiency.

Gas wire horsepower is related to gas motor brake horsepower by the motor efficiency, e_m (see Figure 4):

$$(36)\quad \text{gas wire horsepower} = \text{gas motor brake horsepower}/e_m$$

in which e_m is in decimal %.

Motor efficiencies are a function of the motor and the load. Typically, full-load motor efficiencies may range from 0.90-0.95. If determinations of the gas wire horsepower and gas motor brake horsepower are made directly, then e_m can be calculated from Equation 36.

As with the other horsepower terms, it would be advisable to define a standard gas wire horsepower. It could be defined as the standard gas brake horsepower (standard gas motor brake horsepower) divided by a standard motor efficiency, e_{ms}. A value of 0.92 might be reasonable for e_{ms}.

Turbine Horsepower

Turbine horsepower is that part of the total aeration system power requirement that is specifically related to the operation of a mechanical device such as a turbine or pump. It refers to the general class of turbine horsepower determinations, including water, delivered, brake, and wire horsepower (see Figure 5). With the exception of wire horsepower, these determinations are normally made with a brake, torque cell, or other torque measuring device. The following section discusses various methods used to measure shaft horsepower.

Shaft Horsepower Determinations

Shaft horsepower can be determined with a number of torque measuring devices. One of the oldest is the Prony brake (8), named after G.C.F.M. Richy, Baron de Prony (1755-1839). It measures the torque required to stop the rotation of a moving flywheel. A lever arm is attached to a braking arrangement that grips the flywheel. The restraining force required at the end of the lever arm is measured with a scale of some type. The torque is then calculated as follows:

$$T_q = F\ L \tag{37}$$

in which:

T_q = torque, ft·lb
F = force required at the end of the lever arm, lb
L = length of the lever arm, ft

The power required is related to the torque and rpm by the following expression:

$$P = 2\pi T_q N_r/33{,}000 \tag{38}$$

in which:

P = power, hp
N_r = rpm

Using this principle, actual determinations of brake horsepower are made with the flywheel rigidly mounted to the casing of the unit being tested, such as a motor, blower, or gear box. The unit is mounted on a gimbal, which allows it to rotate without appreciable power loss, and is operated under the desired test condtions. The torque required to stop the casing of the unit from revolving is the brake or shaft horsepower. As might be imagined, this type of arrangement can be very difficult to set up.

The torque cell is another method of measuring brake horsepower and is probably much easier to apply in actual practice. This method works on the principle that the strain induced in a rod or hollow cylinder is proportional to the applied torque (8). Usually, a thin-walled metal cylinder is used as the shaft or is connected to the shaft of the unit being tested. Strain gauges attached at 45° angles on the surface of the cylinder measure the strain developed under load (multiple strain gauges may be installed and connected so that the axial and transverse loads will cancel out in the final readout circuit). The torque is related to the strain at 45° angles by:

$$T_q = (1/12)\ \pi G_o(r_o^4 - r_i^4)\ \ \xi_{45}/r_o \tag{39}$$

in which:

G_o = shear modulus of elasticity, lb/in.^{2}
r_o = outside radius of the cylinder, in.
r_i = inside radius of the cyliner, in.
ξ_{45} = strain at 45° angles

Power can then be calculated by Equation 38.

Turbine Delivered Horsepower

For turbine equipment such as surface aerators and submerged turbines, delivered horsepower is the power required at the output shaft of the gear box to turn the shaft and impeller at the desired rpm in water under actual conditions. This power is also referred to as turbine shaft or turbine water horsepower. The turbine delivered horsepower is normally measured with a brake, torque cell, or other measuring device, although it is not usually referred to as a brake horsepower.

As with the other horsepower terms, it would be advisable to define a standard turbine delivered horsepower to be used when reporting clean water data for advertising or comparison purposes. Since water temperature would seem to be the most important variable affecting the turbine power draw, it might be appropriate to define standard turbine delivered horsepower as the turbine shaft horsepower required at a water temperature of 68°F. While it is realized that it would be impossible to run all power tests at 68°F, it is hoped that there are correlations existent in the field today which might relate power draw at a given temperature to that at 68°F.

Turbine Brake Horsepower

The brake horsepower associated with turbine equipment is similar to the brake horsepower associated with gas delivery equipment. The gear box brake horsepower is

Figure 3. Static Head Bubbler Device

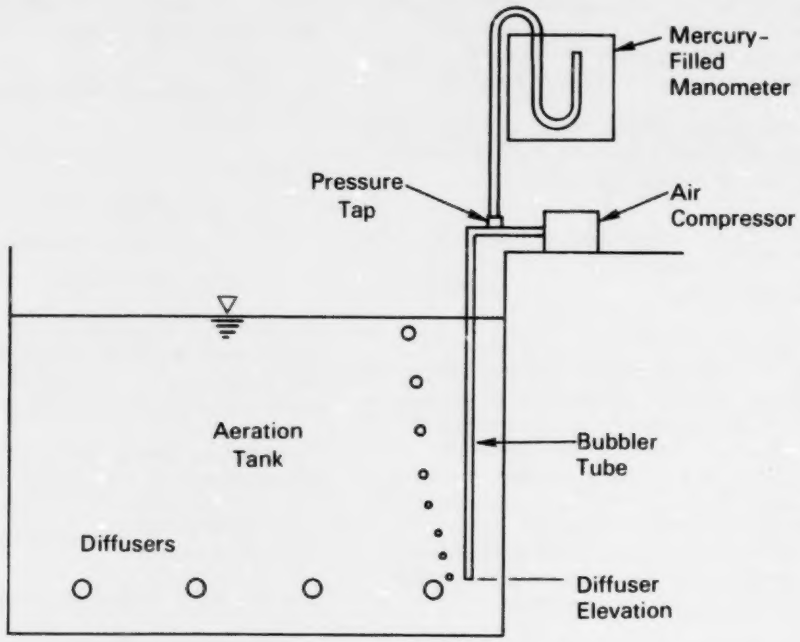
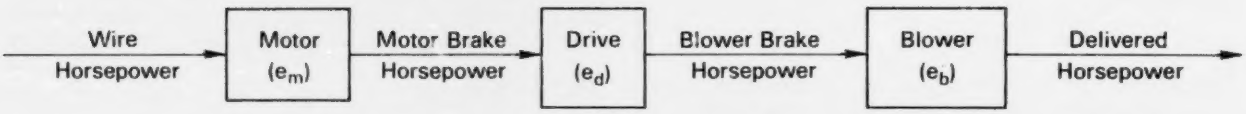
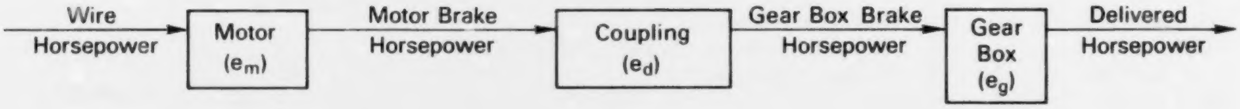

Mercury-
Filled
Manometer
Pressure
Tap
Air
Compressor
Aeration
Tank
Bubbler
Tube
Diffusers
Diffuser
Elevation

Figure 4. Gas Horsepower Schematic


Wire
Horsepower
Motor
(e_m)
Motor Brake
Horsepower
Drive
(e_d)
Blower Brake
Horsepower
Blower
(e_b)
Delivered
Horsepower

Note: Efficiencies are in parentheses.

Figure 5. Turbine Horsepower Schematic


Wire
Horsepower
Motor
(e_m)
Motor Brake
Horsepower
Coupling
(e_d)
Gear Box Brake
Horsepower
Gear
Box
(e_g)
Delivered
Horsepower

Note: Efficiencies are in parentheses.

generally considered to be the shaft horsepower associated with the input shaft. Furthermore, the motor brake horsepower may not be the same as the gear box brake horsepower due to the drive or coupling employed between the motor and the gear reducer (see the "Gas Brake Horsepower" section of this paper and Figure 5).

In a manner similar to gas delivery devices, the gear box brake horsepower is related to turbine delivered horsepower by a gear box efficiency, e_g:

$$\text{(40)} \quad \begin{matrix}\text{turbine gear box}\\ \text{brake horsepower}\end{matrix} = \text{turbine delivered horsepower}/e_g$$

in which e_g is in decimal %.

The gear box efficiency is a function of the gear box design (including the number of gear reductions) and the load. The losses in a gear box are basically of two different types, a fairly constant churning loss and a bearing loss which is a function of load. Generally, speaking, these gear boxes are very efficient. Rating curves are normally available from the manufacturer. A range of full-load gear box efficiencies applicable throughout the field today might be 0.94-0.96. When measurements of the powers in Equation 40 are made directly, the efficiency, e_g, can be calculated.

The turbine motor brake horsepower is related to the turbine gear box brake horsepower by the coupling or drive efficiency, e_d:

$$\text{(41)} \quad \begin{matrix}\text{turbine motor}\\ \text{brake}\\ \text{horsepower}\end{matrix} = \text{turbine gear box brake horsepower}/e_d$$

in which e_d is the decimal %.

The definition of the term e_d, as well as its range, is the same as that discussed in the section on gas brake horsepower. Again, e_d can be determined if the horsepowers in Equation 41 are measured directly.

As with the other horsepower terms, it would be appropriate to define a standard turbine brake horsepower. The standard turbine brake horsepower could be related to the standard turbine delivered horsepower by a standard gear box efficiency, e_{gs}, and a standard drive efficiency, e_{ds}. Possible values for both of these efficiencies might be 0.95, although many others could be proposed.

Actual measurements of either turbine motor brake horsepower or turbine gear box brake horsepower can be made with the methods in the "Shaft Horsepower Determination" section of this paper.

Turbine Wire Horsepower

Turbine wire horsepower can be measured directly with a recording wattmeter. It is very important, however, that the wattmeter be zeroed and calibrated accurately before it is used. An ammeter can be used as long as the voltage and power factor are also measured. An expression for calculating 3-phase power from current, voltage, and power factor measurements is:

$$\text{(42)} \quad \begin{matrix}\text{wire}\\ \text{horsepower}\\ \text{(3 phase)}\end{matrix} = 1.73\ EI\ (PF)/746 = (2.319\times10^{-3})EI(PF)$$

in which:

E = voltage, volts

I = current, amperes

PF = power factor

It is always appropriate to use a recorder when making power measurements. During the operation of a mechanical device, there are usually fluctuations in load which can be most easily observed with a recorder.

Turbine wire horsepower is related to turbine motor brake horsepower by the efficiency, e_m (see Figure 5):

$$\text{(43)} \quad \begin{matrix}\text{turbine wire}\\ \text{horsepower}\end{matrix} = \text{turbine motor brake horsepower}/e_m$$

in which e_m is in decimal percent.

As mentioned previously in the section on gas horsepower, a range of full-load motor efficiencies might be 0.90-0.95. If the horsepowers in Equation 43 are measured directly, the e_m can be calculated.

As with gas horsepower determinations it is recommended that a standard efficiency e_{ms}, be used to relate standard turbine wire horsepower to standard turbine brake horsepower (standard turbine motor brake horsepower). As before, a possible value of e_{ms} might be 0.92.

Total Horsepower

The summation of the gas and turbine horsepowers is the total horsepower. Thus, references can be made to total delivered horsepower, total brake horsepower, and total wire horsepower. When reporting clean water horsepower data, it would be appropriate to report the gas and turbine horsepower data separately, as well as the combined total. In the case of data to be used for advertising or comparison purposes, all horsepower results should be reported in terms of standard conditions.

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Oxygen Transfer Data Interpretation: Non-Steady State Clean Water Tests

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Introduction

There appears to be reasonable concensus that the non-steady state test, more or less as described in WPCF Manual of Practice No. 5 (1), can be a useful aid in predicting performance of aeration systems in clean water, and perhaps wastewater, under field conditions. Similar concensus probably exists that opportunity remains for improvement in the accuracy and precision of the predictive process.

This paper discusses experiences and inconsistencies we have encountered with this method, applied to several different diffused aeration devices. It also suggests procedures in data analysis which may improve the accuracy and precision of the results.

A number of areas which should be considered in order to avoid possible deficiencies and potential sources of error in the non-steady state test method are the following:

1. The appropriate assumptions of effective interfacial area, pressure, and gas phase oxygen concentration relationships, their effect on the saturation value, and their variability during the test.
2. The appropriate allowance for DO gradients under steady state conditions and its effect on the available driving force.
3. Discrepancies and/or validity of the methods for measuring liquid phase oxygen concentration, sampling procedures, and their effect.
4. Applicability of the data to field conditions: appropriate corrections to the overall mass transfer coefficient for influential wastewater constituents and other important variables such as temperature, etc.

This paper also reports and compares results obtained by several different non-steady state test models. Agreement and precision of K_La as determined by the log deficit method, the direct method, and a non-linear least squares data analysis method are shown as well as the relationship of saturation values obtained by the direct method, the non-linear least squares method, and actual measurement. Suggested criteria for acceptability of test results are proposed, along with techniques for improvement of the various methods.

Procedure

Test Method and Facility

A description of the test method and facility have been previously described (8); however, some of the more important aspects of the method will be reviewed here.

The data presented are based upon DO values of gravity flow samples obtained using the modified Winkler titration procedure, as outlined in Standard Methods (18). The sampling points were positioned geometrically in the test tank in an attempt to obtain a composite of samples that represented the average oxygen content of the tank at any given time.

An effort was made to reaerate the tank contents until the equilibrium saturation value was attained. In most tests, an equilibrium saturation value was assumed to exist after a period of time of approximately $6/K_La$, as has been suggested by Stukenburg et al. (20).

Piped samples, *in situ* probes, and *in situ* grab samples were evaluated. Good agreement between K_La values determined by probe data and grab samples was achieved, and consistently higher K_La values were obtained for these methods than for the piped samples.

To correct K_La to 20°C, a theta (θ) value of 1.020 was employed. It is recognized that a variety of opinions exist as to the proper value of θ and that additional work should be undertaken to verify its value as it applies to submerged aeration devices.

Data Analysis

All test runs were analyzed by the log apparent deficit vs time method in accordance with the procedure of WPCF Manual of Practice No. 5 (1), except as otherwise noted. Apparent deficit was obtained by subtracting average tank DO concentration at the reference time from the measured equilibrium saturation value, as described by Boyle et al. (4) and other investigators (9) (10). The apparent overall transfer coefficient, K_La', (as employed in this work) is the value obtained by linear regression analysis of the log apparent deficit vs time data within the range of DO concentrations corresponding to 20-90% of the equilibrium saturation value. As subsequently described, the value thus obtained differs from what is considered to be the true overall transfer coefficient, K_La.

Many of the runs were additionally analyzed and K_La' obtained by the direct method, as described by Stukenberg (20) and Gilbert (9). Computations were made from the same data employed in the log deficit method. The data were analyzed by linear regression analysis; the X-intercept was reported as C^*_∞ and the Y-intercept as dC/dt at C = zero, which, if divided by the X-intercept, yields the apparent K_La'.

A few runs were also analyzed by non-linear least squares analysis of the C vs t data in accordance with the procedure and program suggested by Baillod and Lee (2). This method also yields values of apparent K_La' which differ from the true K_La.

Discussion

Potential Sources of Error in the Non-Steady State Test

Detailed consideration and evaluation have been given to the various areas of controversy and potential sources of error in the non-steady state test method.

Influence of Effective Interfacial Area Distribution, Pressure and Gas Phase O_2 Concentration Relationships, and Assumptions Regarding Them

A number of investigators (3) (5) (14) have considered these relationships simultaneously as a single subject. General recognition exists that the assumptions made regarding them have a significant effect upon the results obtained. The general expression describing mass transfer between liquid and gas phases, as expressed by Sherwood and Pigford (17), is generally accepted:

$$N_A = K_G\,(p-p_o) = K_L\,(C_e-C) \tag{1}$$

where:

N_A = mass transfer per unit of time

K_G = gas film coefficient, mass transfer per unit of time per unit pressure difference

P = solute partial pressure in gas phase

p_o = solute vapor pressure in main liquid phase

K_L = liquid film coefficient, mass transfer per unit time per unit concentration difference

C_e = concentration of solution in equilibrium with solute at partial pressure p

C = concentration of solute in main liquid phase

There appears to be general agreement (6) (12) that in the case of air and water, the liquid film resistance is controlling, and the relationship may be practically considered as:

$$N_A = K_L(C_e-C) \tag{2}$$

However, the applicability of the expression is specifically confined to steady state conditions where C_e and C are constants (17).

This relationship may be expressed in more conventional aeration equipment testing terms as:

$$W = dC/dt = K_La\,(C^*-C) \tag{3}$$

where:

W = dC/dt = transfer rate per unit volume in clean water, m/L^3t

a = interfacial surface area per unit volume, L^{-1}

C^* = DO concentration in equilibrium with gas of composition corresponding to time t, m/L^3

C = DO concentration at time t, m/L^3

In non-steady state testing, a, C*, and C are changing throughout the test, with a resultant and corresponding change in dC/dt (8) (11) (13) (19). The reduction in oxygen absorption rate during the test results in a progressive increase in interfacial area and a change in its distribution during the test, as well as an increase in exit gas phase oxygen concentration with a corresponding increase in the saturation value, C*. However, the expression is considered to accurately describe the transfer phenomenon for incremental periods of time, provided the appropriate and changing values of a and C* are used or accounted for.

Most methods of non-steady state data analysis treat both C* and a as constants in the analysis of the data. A number of investigators (3) (14) (15) have suggested the use of C* values derived from solubility tables which give mid-depth saturation values corrected for temperature, pressure, and an assumed distribution of interfacial area (typically uniform). Others (4) (9) (10) (16) have recommended use of equilibrium saturation values as C* determined by measurement, best fit of the log deficit time relationship, or determination of the X-intercept of the dC/dt plot in the direct method.

Oldshue (14) proposed a method of accounting for interfacial area distribution, pressure, and gas side oxygen concentration which he considered applicable to submerged turbine aerators. It may be expressed in the following form:

(4) $\bar{C}^* = C_s^* [(Y_e/2Y_d) + (p_a + \gamma Z_d)/2p_a]$

where:

$\bar{C}^*$ = effective average DO saturation concentration, m/L^3

C_s^* = surface saturation value, m/L^3

Y_d = mole fraction of oxgyen in the feed gas

Y_e = mole fraction of oxygen in the exit gas

p_a = atmospheric pressure, f/L^2

Z_d = aerator submergence, L

γ = weight density of water, f/L^3

The assumption of uniform distribution of interfacial area with depth is implicit in the expression. Although not specifically defined, it may be inferred that this expression is applicable to clean water testing as well as to field situations. Other investigators (3) (15) have suggested use of the Oldshue equation with minor modifications, or with more specific recommendations as to method of application. All of the above references, however, suggest treating C* as a constant during the test period. Where oxygen transfer efficiency is low (e.g., OTE less than 0.15), or where the ratio of field transfer rate (at a given air rate) to the cleanwater rate is high (e.g., greater than 0.7), the error induced by so doing is of minor consequence, provided the approximately correct value of equilibrium saturation is used in the test and in the field application. The importance of proper selection of the saturation value has been suggested by many (4) (7) (10) (16) and cannot be overemphasized. An error in its selection typically results in a several fold error in K_La' determined by the log deficit method.

Downing and Boon (5) recognized and reported the variability of C* during the non-steady state test and proposed means of accounting for it. Stanton and Bradley (19) advanced a method which assumes uniform interfacial area distribution with depth and plug flow in the aeration zone. They considered it applicable to submerged areators with mechanical agitation.

In view of the importance of the value selected for C* and the widespread use of the simplifying mid-depth assumption, a short digression on that subject is considered appropriate.

Various investigators (4) (9) (16) (19) have recognized that the equivalent saturation value, expressed in terms of tank depth, is a variable, dependent among other things upon the type of device used and its disposition in the system. It is acknowledged that in the applications of some devices under some conditions, the mid-depth saturation value will provide adequate precision, but in the majority of cases it will not. In the special case where interfacial area per unit volume is constant throughout the tank and independent of depth, where mixing characteristics are such that the DO concentration is substantially uniform throughout the depth, and where the net oxygen transfer is substantially zero, the expected equilibrium saturation value can be shown to approximate the mid-depth pressure saturation value.

In this instance, oxygen will be absorbed by the liquid from the gas below this level and extracted by the gas from the liquid above this level. The same kinetic laws apply to both phenomenon, and pressure is linear with depth; therefore, net oxygen transfer ceases (saturation) when the oxygen concentration in the liquid phase reaches a value, in terms of tank depth, where the interfacial area below that reference level just equals the area above it. Since area/unit volume is uniform, this must be mid-depth and, under these conditions, the effective absorptive driving force applied in the lower half just equals the desorptive driving force in the upper half.

In other, more typical cases where interfacial area per unit volume is not uniformly distributed, a saturation value corresponding to some other level (or pressure) will be obtained. We believe that in this case it will be such a level that the integrated product of interfacial area and driving force below this plane will approximate that above it. The saturation value of air and water at this equivalent pressure is believed to approximate C_∞^*, as measured after a long period of aeration. Systems where bubbles subdivide as they rise should tend to have saturation values above mid-depth. Conversely, systems where bubbles coalesce on rising should tend toward saturation values below mid-depth.

Downing and Boon (5) also recognized the variability of C* during the non-steady state test and proposed a method of compensation which appears to be applicable to a wide variety of diffused aeration air devices. Earlier work (8) showed that their expressions may be rewritten as follows:

(5) $$dC/dt = K_La\,(C_\infty^* F_t - C)$$

(6) $$V(dC/dt) = (M_o/M_a)\rho_a Q_a (Y_d - Y_e) = (OTE)_t Q_o$$

(7) $$C^* = C_\infty^* F_t$$

(8) $$t = \ln[C_\infty^*/(C_\infty^* - C]/K_LaF_0$$

where:

F_t = exit gas correction factor, Downing method, at reference time

F_0 = $1 - 0.5(OTE)_0 = 1 - 0.5(dC/dt)_0\,(V/Q_o)$

Subscript 0 = conditions where C = zero

Q_a = volumetric flow rate of air at standard conditions, L^3/t

V = liquid volume, L^3

ρ_a = mass density of air at standard conditions, m/L^3

M_o = molecular weight of oxygen

M_a = molecular weight of air

C^*_∞ = equilibrium saturation value attained by direct method or after a long period of aeration, m/L^3

$(OTE)_t$ = clean water ratio of oxygen transferred to oxygen supplied at reference time

Q_o = mass flow rate of oxygen, m/t

Downing and Boon's derivation included two simplifying assumptions which they consider to be of negligible adverse consequence:

1. The rate of gas flow leaving the surface of the tank was equal to the gas flow introduced into the tank during the test (constant pressure-interfacial area relationship may be inferred).
2. The effective mole fraction of oxygen in the gas phase was considered as the arithmetic average of the mole fraction entering and leaving the tank.

Whatever error may result from assumption 1 may be reduced by employing the following relationships regarding mole fraction of oxygen in the gas phase:

(9) $$Y_d = 0.21$$

(10) $$Y_e = 0.21[1-(OTE)_t]/\{0.79+0.21[1-(OTE)_t]\}$$

Under these conditions, the following may be obtained for Equations 5, 7, and 8:

(11) $$dC/dt = K_La\,(C^*_\infty F'_t - C)$$

(12) $$C^* = C^*_\infty F'_t$$

(13) $$t = \ln[C^*_\infty/(C^*_\infty - C)]/K_La\,F'_0$$

where:

F'_t = $[1-0.605(OTE)_t]/[1-0.21(OTE)_t]$

F'_0 = $[1-0.605(OTE)_0]/[1-0.21(OTE)_0$

F' = exit gas correction factor, modified Downing method

It may be shown that under the assumption of Downing, dC/dt is linear with respect to C. When the mole fraction of exit gas is corrected for absorption of oxygen, dC/dt vs C may be shown to depart from linearity. The fact that this departure is of minor consequence is indirectly suggested by a preponderance of investigators (8) (9) (20) who have observed in clean water tests that, in fact, dC/dt vs C is substantially, though perhaps not precisely, linear.

It is felt that the error induced by the assumption of linearity in the modified Downing equations is of less consequence than the error induced by the assumption of Downing that gas flow in equals gas flow out.

As a consequence, in the direct method of analysis, the X-intercept of the plot represents the true saturation value of the system under test at that air rate and at an oxygen concentration of 21%. The Y-intercept represents the true dC/dt at C equal to zero, but at an oxygen concentration of less than 21%. Thus, the Y-intercept divided by the X-intercept yields an erroneous or apparent K_La'. The true K_La may be obtained by dividing the Y-intercept by C^* at C equal to zero, which may be obtained from Equations 7 or 12. This is equivalent to dividing by F_0 in the Downing method and by F'_0 in the modified approach.

The substantial linearity of dC/dt with C is an indication that the rate of absorption may be considered first order with respect to C. It follows that the relationship of the log apparent deficit vs time is comparably linear, which is consistent with reported observations. Apparent deficit denotes the deficit obtained by subtracting C from the saturation value attained after long periods of aeration, C^*_∞. This value should either be measured directly or estimated from the model fit to the data.

Although Downing (5) was not specific as to the application of clean water data to field conditions, it is inferred that the corrected K_La may be used and that the field saturation value may be corrected for oxygen concentration at the designed or estimated dC/dt as follows:

(14) $$C^*_f = (C^*_{\infty 20})\,(C^*_{sbT}/C^*_{sb20})\,(p_f/29.92)\,\beta F_{tf}$$

or:

(15) $$C^*_f = (C^*_{\infty 20})(C^*_{\infty sbT}/C^*_{sb20})(p_f/29.92)\beta F'_{tf}$$

where:

C^*_f = *in situ* saturation value at steady state field conditions, m/L^3

C^*_{sbT} = book saturation value at atmospheric pressure and reference temperature, m/L^3

C^*_{sb20} = book saturation value at atmospheric pressure and 20°C, m/L^3

p_f = field barametric pressure, f/L^2

Subscript f = at field conditions

β = beta factor

Since dC/dt is dependent upon C^*_f, a reasonably correct solution may be obtained through trial and error; however, a first estimate of dC/dt which accounts for α, β, C_f, barometric pressure, and temperature may be close enough for all practical purposes.

To evaluate the comparative importance of gas phase oxygen depletion, various methods of analysis, as shown in Appendix A, were applied to test data from four different aeration systems operating over a wide range of transfer efficiencies. These results were then compared with the results obtained by the method we have previously used and described in which no correction for gas phase oxygen depletion is made.

These results are in no way suitable for comparing the performance of the various devices with one another as they are at different and non-comparable depths, transfer rates, dispositions, etc. The different systems were selected since they may be expected to have different pressure-interfacial area relationships. The devices tested included ceramic plates, fixed orifice diffusers, static aerators, and perforated aeration tubing.

In applying the test data to the field conditions, it was assumed that $\alpha = 0.8$, $\beta = 0.9$, $C_f = 2.0$ mg/l, T = 26°C, and p = 29.0 in. Hg. Details of the test results are summarized in Table 1. The equations and data used to construct Table 1 are shown in Appendices A and B, respectively.

In the computation of equivalent saturation depth, D_E, the surface saturation value was obtained by aerating 1-liter samples of tank water. These values were typically 8.9 mg/l at standard conditions.

A comparison of the various methods may be made by expressing the parameters of interest as ratios. Although none of the methods are considered to accurately describe conditions as they exist, we believe the modified Downing method represents a better description of existing conditions from a theoretical standpoint than do the others. Consequently, the K_La and $(OTE)_f$ data have been expressed as ratios of the other methods to the modified Downing method and are shown in Appendix C.

The data indicate, and it is reasonable to expect, that a relationship between these ratios and the clean water oxygen transfer efficiency at standard conditions, $(OTE)_{SC}$, exists. Figure 1 is a plot of the ratios of K_La and $(OTE)_f$

Table 1. Results Obtained by Various Methods of Analysis of Five Data Sets

Run	Parameter	Uncorrected	Mid-Depth Exit Gas	Downing	Downing Modified
34C	C^*_{20}	10.46	11.30	10.46	10.46
	D_E	0.29	0.45	0.29	0.29
	$(K_La)_{20}$	11.12	9.10	12.27	12.05
	C^*_f	9.13	8.18	7.77	7.87
	$(OTE)_f$	0.0997	0.0942	0.1028	0.1023
3F	C^*_{20}	9.71	10.56	9.71	9.71
	D_E	0.24	0.49	0.24	0.24
	$(K_La)_{20}$	10.65	8.06	11.27	11.15
	C^*_f	8.46	7.59	7.38	7.42
	$(OTE)_f$	0.0544	0.0474	0.0552	0.0550
2E	C^*_{20}	11.21	10.65	11.21	11.21
	D_E	0.41	0.31	0.41	0.41
	$(K_La)_{20}$	18.81	24.02	23.15	22.41
	C^*_f	8.76	8.77	7.88	8.03
	$(OTE)_f$	0.2042	0.2603	0.2183	0.2168
1G	C^*_{20}	10.17	10.88	10.17	10.17
	D_E	0.34	0.53	0.34	0.34
	$(K_La)_{20}$	2.47	2.00	2.56	2.54
	C^*_f	8.63	7.95	7.80	7.83
	$(OTE)_f$	0.0388	0.0344	0.0385	0.0384
10HD	C^*_{20}	10.50	9.86	10.86	10.86
	D_E	0.61	0.37	0.61	0.61
	$(K_La)_{20}$	0.186	0.222	0.205	0.201
	C^*_f	7.95	8.21	7.81	7.89
	$(OTE)_f$	0.0979	0.1119	0.1009	0.1002

D_E = the equivalent fraction of tank submergence corresponding to the saturation value of the models employed.

Figure 1. Uncorrected and Downing K_La and $(OTE)_f$ vs Modified Downing

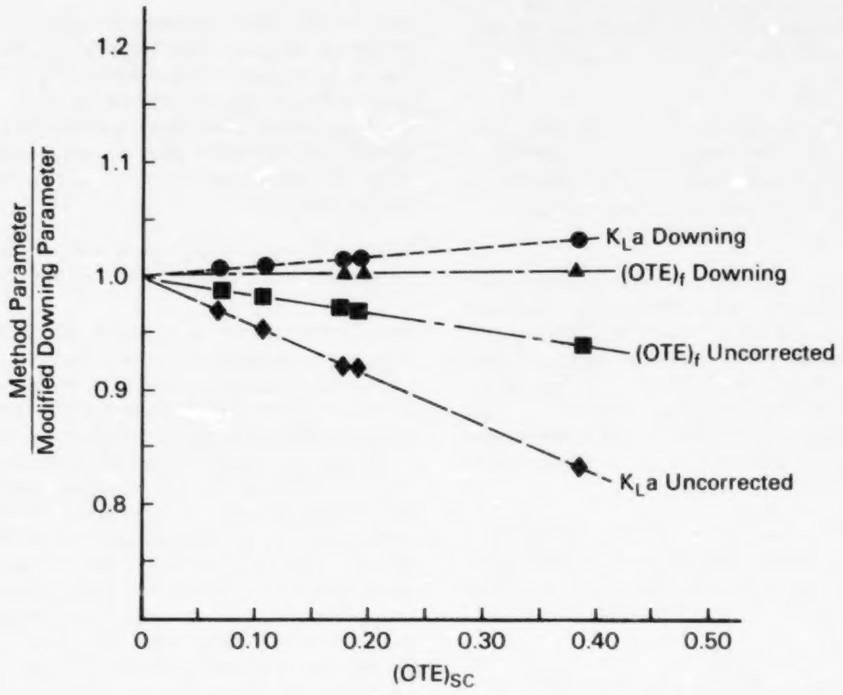
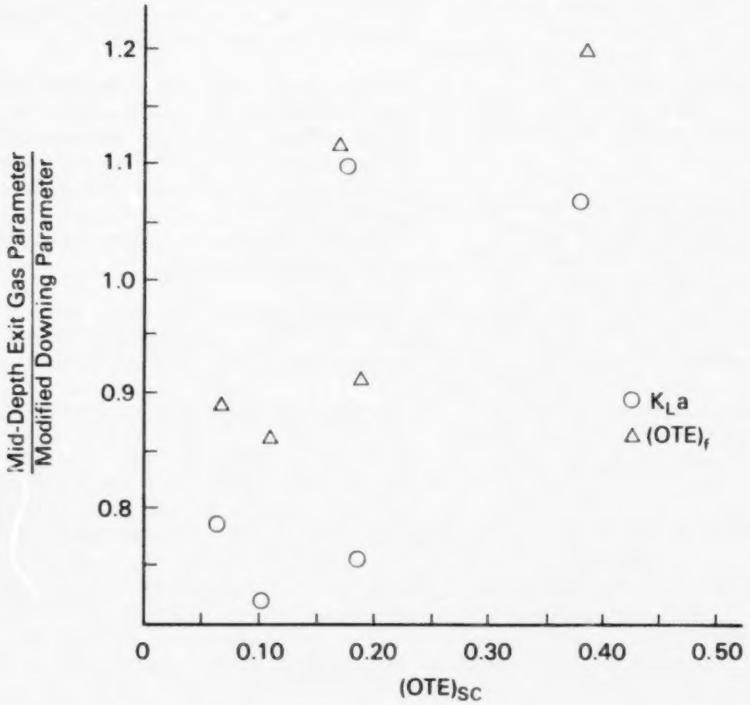


Figure 2. Mid-Depth Exit Gas K_La and $(OTE)_f$ vs Modified Downing



of the uncorrected and the Downing methods to the modified Downing method vs $(OTE)_{SC}$. As might be expected, the relationship is well defined. In terms of $(OTE)_f$ values, the difference between the Downing and modified Downing method is less than 1% up to $(OTE)_{SC}$ values of 0.4.

The difference in $(OTE)_f$ values by the uncorrected method and the modified Downing method is greater than 6% at an $(OTE)_{SC}$ of 0.4 under the field conditions assumed. This is considered significant and adequate justification for making the correction.

Figure 2 plots the ratios obtained by the mid-depth exit gas method. It may be seen that a well defined relationship with $(OTE)_f$ and K_La does not exist, and differences between the methods are great. Ratios of $(K_La)_{20}$ obtained by the two methods range from 0.72 to 1.10. Ratios of $(OTE)_f$ for the two methods range from 0.86 to 1.2. This, in itself, is not evidence of the value of one method over the other, but it suggests that careful analysis should be made in selecting the method to use.

It is our opinion that the mid-depth exit gas method is defective and that the general approach of Downing (5) or some modification thereof is preferable. The principle defects of the mid-depth exit gas method are considered to lie in two areas.

The first, which has been previously discussed, is that mid-depth saturation conditions are typically not attained. In the runs reported here, the saturation values, expressed as a fraction of submerged depth, range from 0.24 to 0.61. For a 20-ft submergence this corresponds to a ratio of saturation values of about 1.19. The corresponding ratios of K_La' values determined by the log deficit method will usually be considerably greater than this. The tendency of this defect will be to yield erroneously low K_La values where the true saturation value is less than mid-depth.

The other area of suggested defect is that the saturation value is considered to be a constant at the gas phase oxygen concentration under the hypothetical conditions of C equal to zero. In tests where oxygen transfer efficiency is high, erroneously low C^* values are used, which yield erroneously high K_La values. An additional disadvantage of the method is that C^* may not be obtained without knowledge of K_La and vice versa, and so trial and error or some more sophisticated method is required for the solution. It is further complicated by the fact that for each assumption a linear regression analysis is required on a new set of deficits.

Two additional, though lesser defects, which exist in the mid-depth exit gas method are considered to be present in the Downing (5) or modified Downing approaches as well. Since significant oxygen is being absorbed early in the test, the interfacial area in the upper part of the tank is less than it is at the time C^*_∞ is measured. This results in the C^* values early in the test being slightly low, which produces corresponding high K_La values.

The second defect results from the assumption of an arithmetic average effective oxygen concentration, rather than a more descriptive model. This is believed to produce results in the opposite direction, i.e., high C^* and low K_La values. Downing's opinion that the error induced is normally negligible appears reasonable to us in the range of submergence and transfer efficiencies normally encountered.

Effect of Oxygen Enrichment and Oxygen Concentration Gradients Throughout the Tank

Most of the models or methods considered suggest the assumption of uniform oxygen concentration in the liquid phase during clean water tests, in field applications, and in selecting the driving force for computation of oxygen transfer efficiency. Such conditions rarely, if ever, are obtained, but in well-mixed systems the resultant error is minor. Significant errors, however, may result in less well-mixed systems. In methods or models that provide a representative average oxygen concentration of the tank, the error that results in K_La due to this cause is considered to be minor. The rate of oxygen absorption is properly accounted for since the average tank oxygen concentration is employed. The computed driving force is considered to be near the correct value since the average concentration of the tank at any time probably does not differ greatly from the liquid concentration in the aeration zone. It is further believed, however, that significant errors may occur when these results are applied to field designs, or in the computation of oxygen transfer efficiency at standard conditions.

In cases where significant DO concentration differences exist, we believe the field driving force should be computed on the estimated average DO concentration in the aeration zone, rather than the minimum value desired, as is typically done. Similarly, computed oxygen transfer efficiencies which use zero DO concentration for C are considered to be fictitious since they are based on unobtainable conditions.

The non-steady state test does not give quantitative insight as to the correction that should be applied to compensate for DO gradients in the aeration zone.

Application of Clean Water Data to Field Conditions

As has been shown in clean water testing, transfer rates may be obtained either as the product of the apparent K_La' and apparent deficit, or as the product of the corrected K_La and the deficit obtained using the saturation value corrected for gas side oxygen depletion. In applications to field conditions, however, the fraction of oxygen transferred at any given air rate is different than in clean water. As a consequence, the apparent K_La' determined in the clean water test is not directly applicable to field conditions. Rather, the corrected K_La should be used along with the field saturation value estimated from the clean water tests and adjusted for other known or assumed field conditions. We consider the appropriate starting point to be the saturation value obtained in clean water tests at the design air rates, submergence, disposition, and geometry. Beta, or the ratio of wastewater to clean water oxygen solubility, may be determined or is frequently assumed. The corrections for barometric pressure and temperature may be made proportionally and in accordance with acceptable temperature solubility tables, respectively. The correction for reduced gas phase oxygen concentration may be made as previously suggested (Equations 14 or 15) at estimated field transfer efficiencies or by trial and error.

This method does not rely upon absolute saturation values, but rather upon the relative solubility at various temperatures which is not in controversy and, if in error, would constitute a minor difference in the predicted results. The driving force, $C_f^* - C_f$, may be obtained by using either an assumed C_f or an estimated C_f which accounts for an estimated or determined DO gradient in the aeration zone, e.g., $C_f = C_{min.} + \Delta C_f/2$:

where:

$C_{min.}$ = minimum DO in the aeration zone under steady state conditions, m/L^3

ΔC_f = field DO rise in the aeration zone under steady state condtions, m/L^3

Comparative Results of Methods of Parameter Estimation

In an attempt to evaluate the agreement of results obtained by the direct and the log deficit methods, the data from 43 runs involving fixed orifice diffusers were analyzed by each method and the results compared. Similar comparisons were made with 41 runs involving three different diffusion systems. The results are summarized in Table 2 and are plotted as Figure 3.

Table 2. Direct vs Log Deficit Analysis

Parameter		Fixed Orifice	Various Devices
$(K_La')_D/(K_La')_{LD}$	Mean	0.990	1.000
	Std. Dev.	0.070	0.090
$[(OTE)_0]_D/[(OTE)_0]_{LD}$	Mean	0.995	1.001
	Std. Dev.	0.050	0.067
$(C_\infty^*)_D/(C_\infty^*)$	Mean	1.006	1.003
	Std. Dev.	0.026	0.028

LD = Log Deficit Method.
D = Direct Method.
NLLS = Non-Linear Least Squares Method.

Two runs in the 41 run series were omitted from the analysis since the direct method indicated saturation values obviously in error. One had an apparent equivalent saturation value of 0.75 tank depth, and the other slightly less than surface saturation. With this exception, agreement between saturation values determined by the direct method and by measurement was excellent and reproducibility appears to be acceptable. The agreement between K_La' and $(OTE)_{SC}$ values determined by the direct and log deficit methods was also excellent; however, standard deviations indicate a substantial lack of reproducibility. The data disclose that one or both methods, as applied to our data, are lacking in precision. They do not disclose which. Some data have been presented (8) which suggest that the precision of the two analytical methods may be comparable. (Note: Subsequent analysis of other more recent data obtained since the Asilomar Workshop gives some indication that the log deficit method is more precise than the direct method.)

In an effort to further understand the variability of the two methods, the dependence of the K_La' ratios upon several potentially sensitive variables was examined. Figures 4, 5, and 6 show the results of these analyses. It may be concluded from these figures that lack of dependence is shown so far as $(OTE)_{SC}$ and K_La' are concerned, but reasonably good correlation exists with respect to the ratio of the estimated and measured values of C_∞^*. This suggests the possibility that the variability of calculated vs observed saturation values contributes to the variability of K_La' in an orderly fashion. This, we believe may partially explain the observed phenomenon of better reproducibility in the $(OTE)_0$ values than in the K_La' values. It also suggests that the variability of the saturation values by the two methods, though minor, may contribute significantly to the variation of $(K_La')_T$.

In an effort to further explore the relative precision of different methods of data analysis, K_La' and C_∞^* for five of the six runs given in Appendix B were analyzed by the log deficit, the direct, and the non-linear least squares methods, as previously described. The values obtained are given in Table 3.

To appraise the degree of agreement and relative precision of the three methods, the ratios of apparent K_La' by each method to the other were determined and the mean values and standard deviations of the values obtained for the five runs were compared. These results are summarized as Table 4 and plotted as Figure 7. It seems apparent that in these five limited cases, there is good general agreement between the methods; however, the relatively large standard deviations (0.111) in the case of the direct-log deficit ratios indicates a lack of precision in one of the methods. Better precision is indicated in the cases of the NLLS/LD and Direct/NLLS by a standard deviation in each case of approximately half of the above.

Figure 3. K_La, $(OTE)_{SC}$, and C^*_∞ Computed by Direct Method Divided by Log Deficit Method

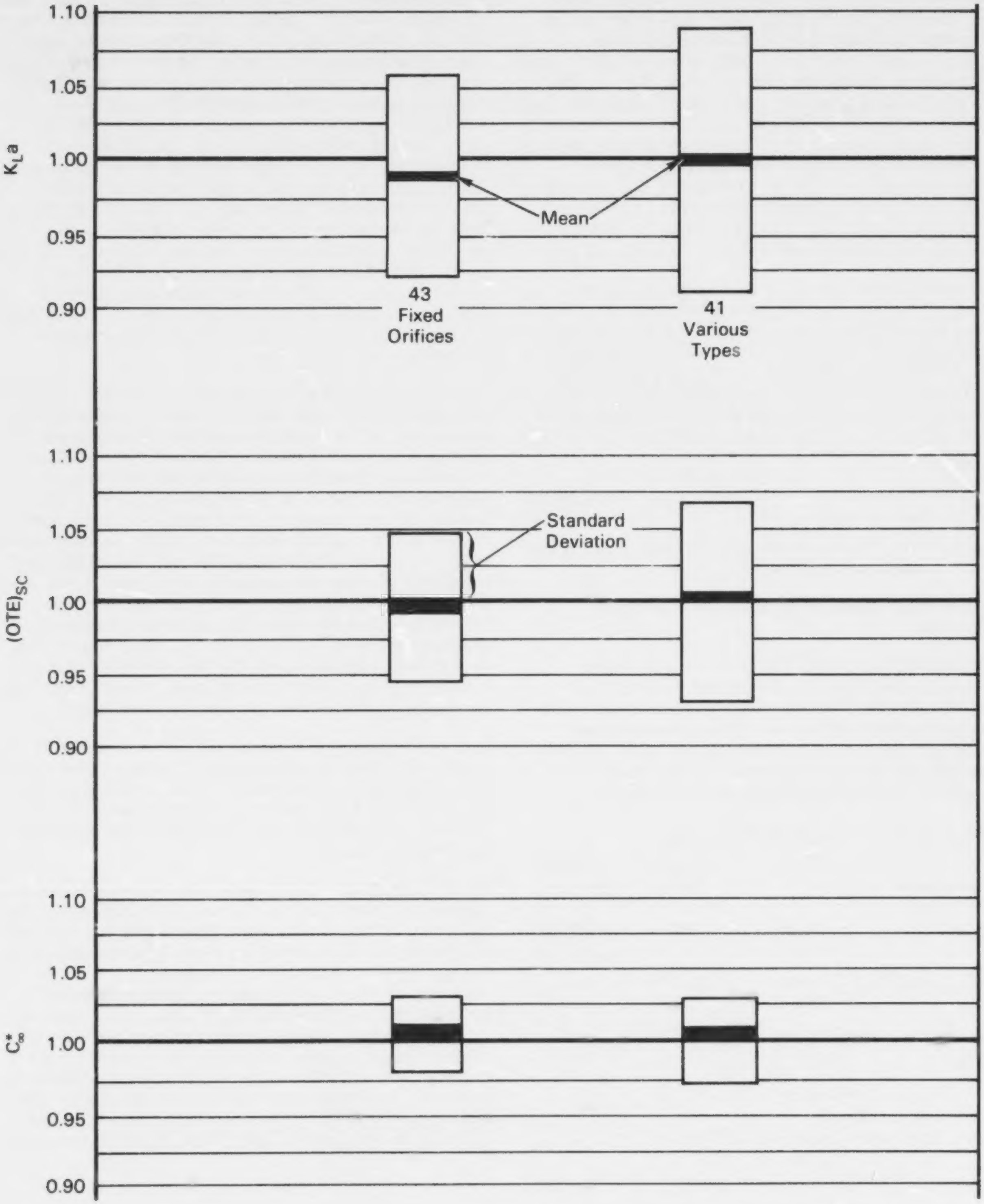


Figure 4. Correlation of $(K_La')_D/(K_La')_{LD}$ vs $(K_La')_{LD}$

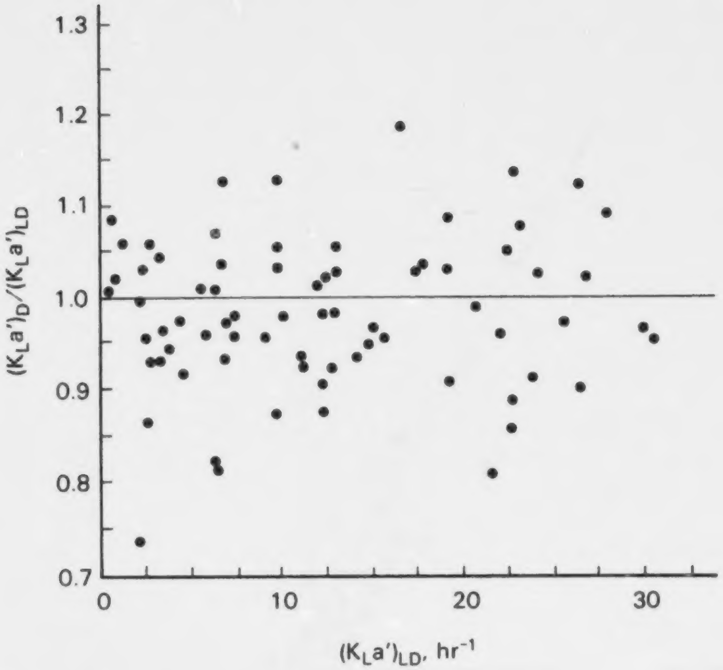


Figure 5. Correlation of $(K_La')_D/(K_La')_{LD}$ vs $(OTE)_{SC}$

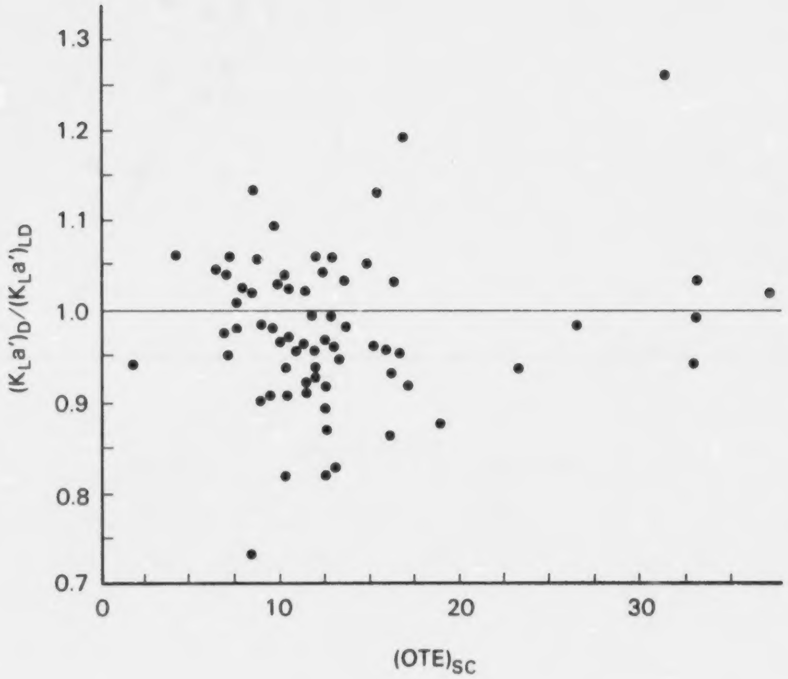


Figure 6. Correlation of $(K_La')_D/(K_La')_{LD}$ vs $(C^*_\infty)_D/(C^*_\infty)_{LD}$

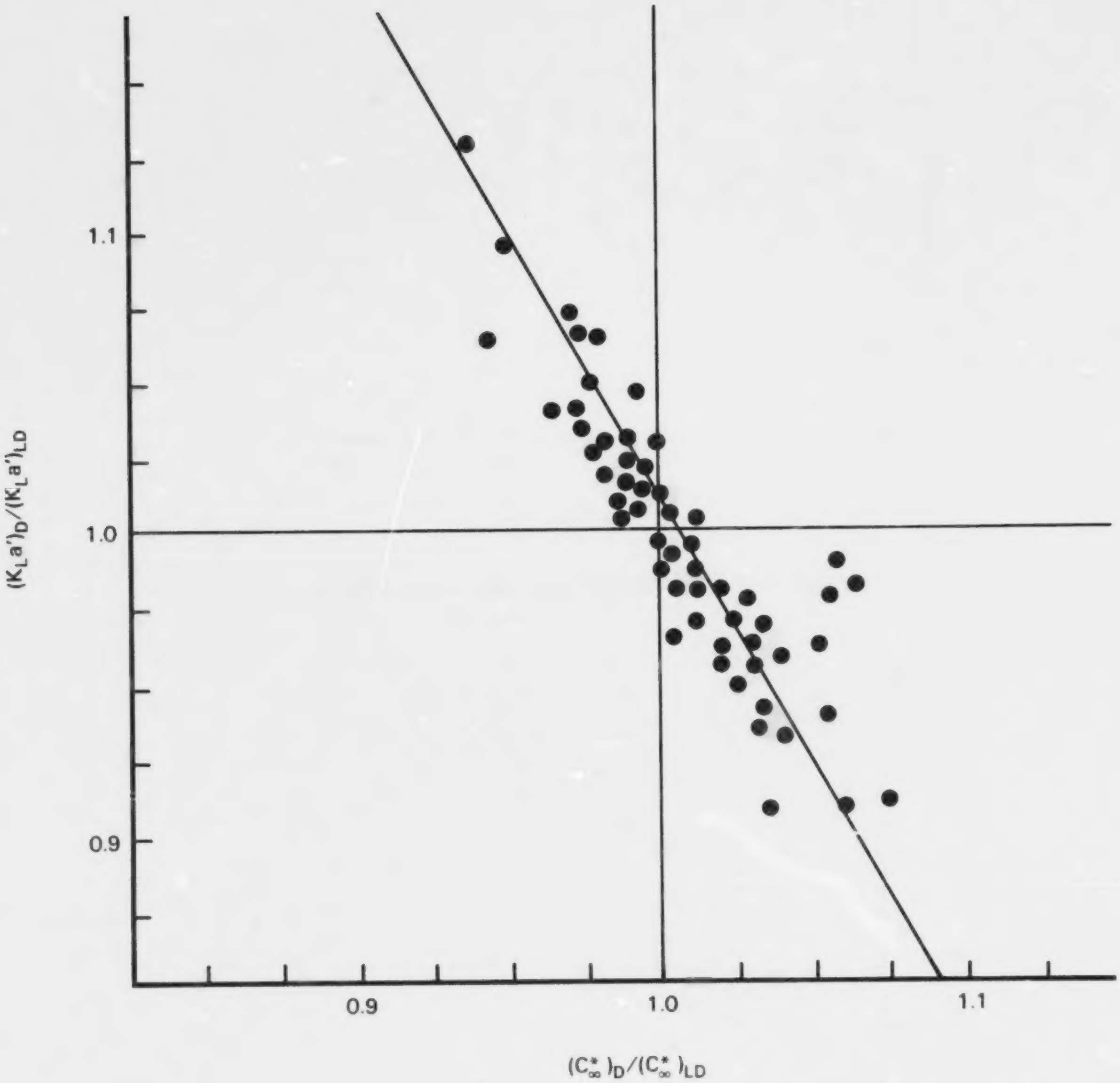


Figure 7. Ratios of K_La' of Five Sets by Various Methods of Analysis

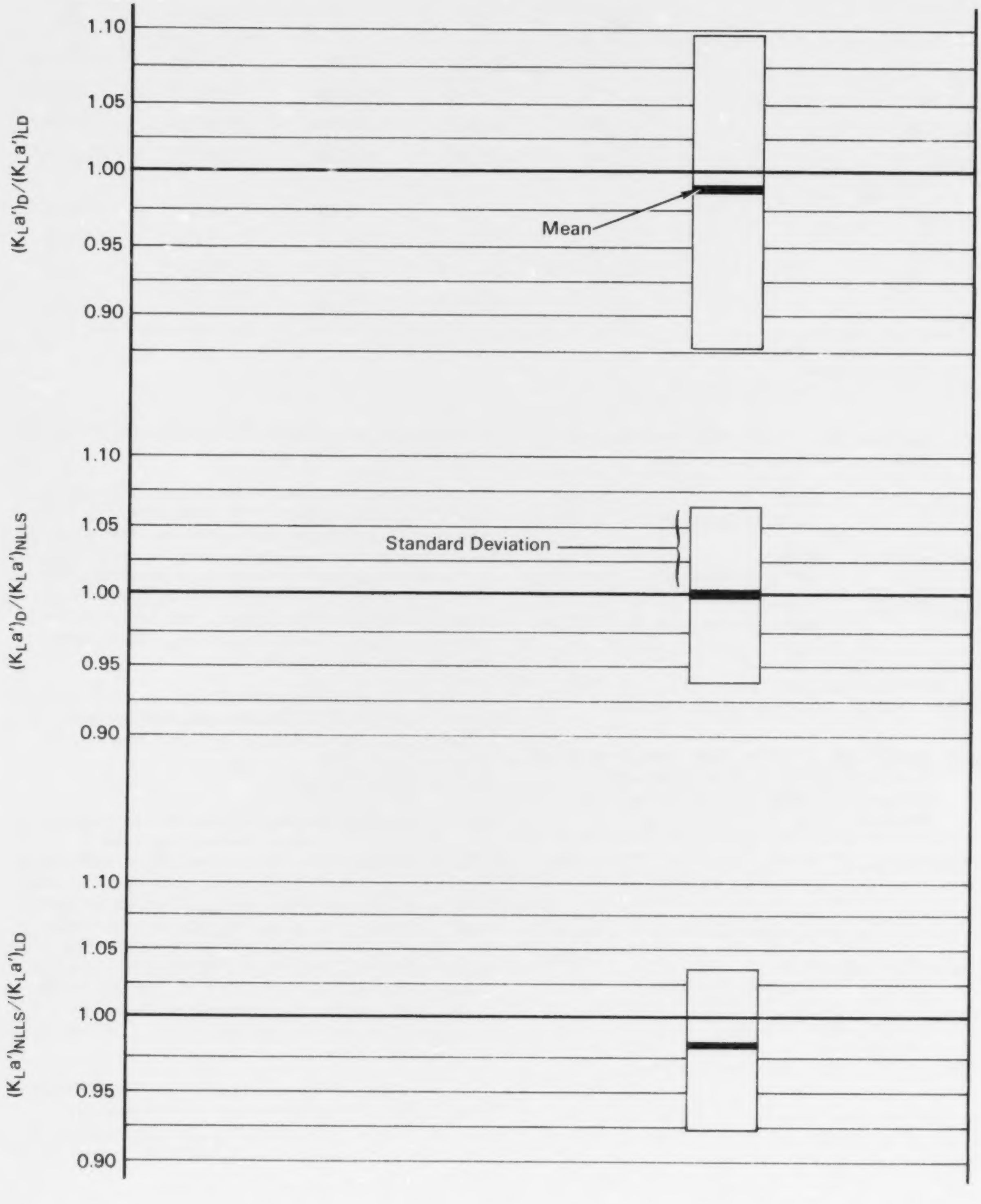


Table 3. Values of K_La' and C^*_∞ of Five Data Sets by Various Methods of Analysis

	Run No.				
	34C	3F	2E	29E	1G
$(K_La')_{LD}$, hr^{-1}	0.1611	0.1564	0.3000	0.09764	0.03805
$(K_La')_{LD}$, hr^{-1}	0.1425	0.1458	0.3420	0.08833	0.04033
$(K_La')_{NLLS}$, hr^{-1}	0.1476	0.1487	0.3174	0.09509	0.03843
$(K_La')_{Mean}$, hr^{-1}	0.1504	0.1503	0.3198	0.09369	0.03894
$(C^*_\infty)_{LD}$, mg/l	11.64	10.58	11.43	10.40	10.75
$(C^*_\infty)_{D}$, mg/l	12.27	10.76	11.11	10.82	10.55
$(C^*_\infty)_{NLLS}$, mg/l	12.11	11.09	10.94	10.62	10.45
$(C^*_\infty)_{Mean}$, mg/l	12.01	10.81	11.16	10.61	10.58

LD = Log Deficit Method.
D = Direct Method.
NLLS = Non-Linear Least Squares Method.

Table 4. Ratios of K_La' of Five Data Sets Determined by Various Methods of Analysis

$(K_La')_D/(K_La')_{LD}$	Mean	0.984
	Standard Deviation	0.111
$(K_La')_D/(K_La')_{NLLS}$	Mean	1.000
	Standard Deviation	0.0651
$(K_La')_{NLLS}/(K_La')_{LD}$	Mean	0.9814
	Standard Deviation	0.0539

LD = Log Deficit Method.
D = Direct Method.
NLSS = Non-Linear Least Squares Method.

Table 5. Ratios of K_La' of Five Data Sets Determined by Various Methods of Analysis and Compared [illegible]ean Values of the Three Methods

$(K_La')_{LD}/(K_La')_{Mean}$	Mean	1.014
	Standard Deviation	0.544
$(K_La')_D/(K_La')_{Mean}$	Mean	0.9931
	Standard Deviation	0.0565
$(K_La')_{NLLS}/(K_La')_{Mean}$	Mean	0.9931
	Standard Deviation	0.0131

LD = Log Deficit Method.
D = Direct Method.
NLLS = Non-Linear Least Square Method.

It was speculated that since all three methods are in good general agreement, the mean value of K_La' of the three methods, for a given data set, might represent a more reasonable prediction of the value of K_La' than any of the individual values determined.

If this speculation is valid, a comparison of the values obtained by each method to the mean should give some indication of the relative precision of the three methods. Such a comparison was made and is shown as Table 5 and plotted as Figure 8. The standard deviation of the NLLS method is 0.013 vs approximately 0.055 for the other two methods. This suggests that, based on the above supposition, the NLLS analysis is more precise than the other two methods.

In view of the limited number of runs evaluated, this is not considered to be conclusive evidence, but rather an indication of a subject worthy of further investigation.

Acceptability of Test Results

When and if sufficient data are available to establish an acceptable level of precision for the non-linear least squares method or other equally satifactory methods as applied to non-steady state testing, such methods might prove to be acceptable without requirements for qualification. In the meantime, on the basis of tests conducted, the authors do not have a high level of confidence in the results of any one test analyzed by either the direct or the log deficit methods of analysis. The level of confidence can be improved by requiring some degree of agreement between the two methods for qualification of test results.

Since good general agreement and comparable precision of the methods have been shown, a disagreement between them in any particular set of data indicates an erroneous K_La' was obtained by one or both methods,

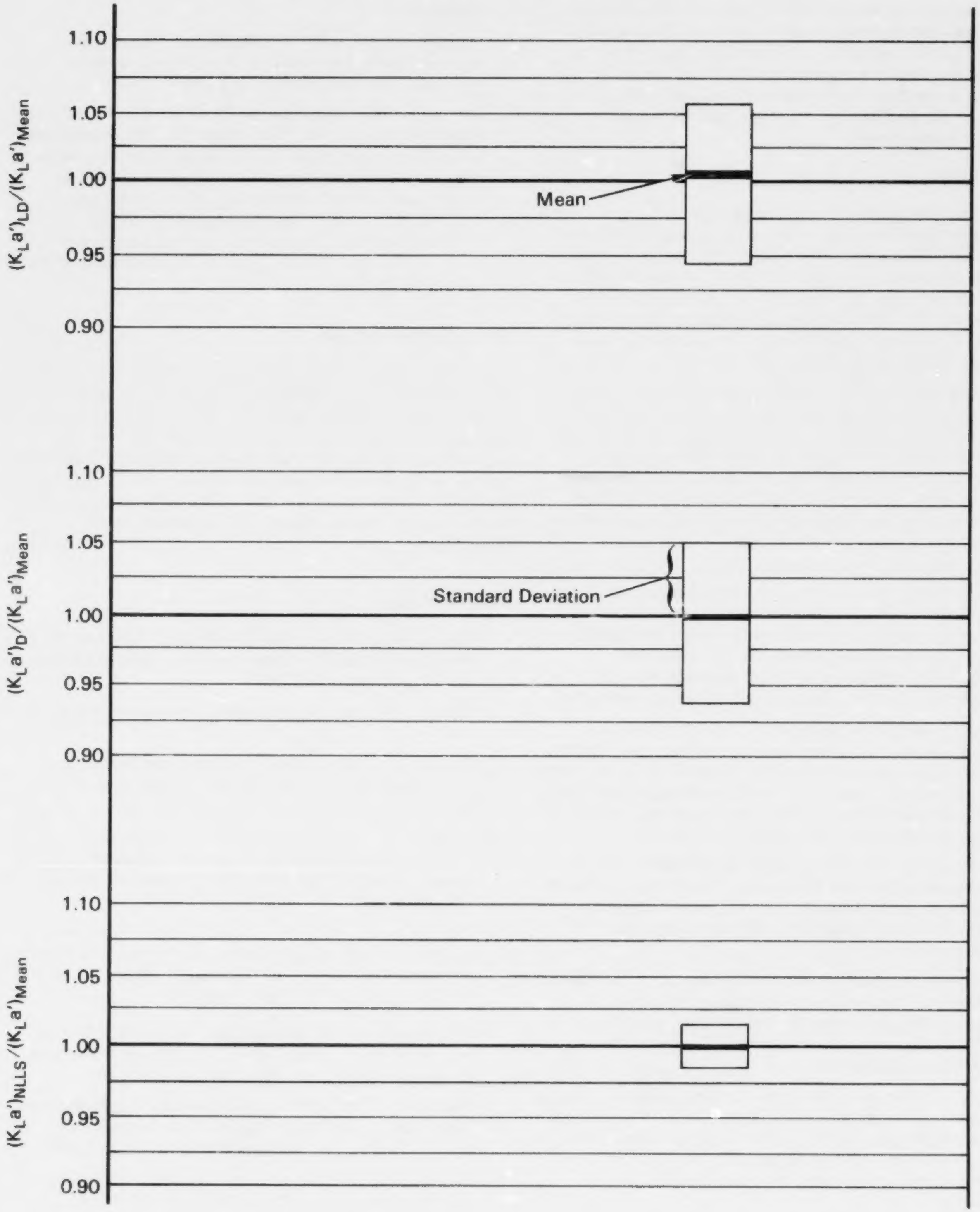


Figure 8. Ratios of K_La' of Five Data Sets Determined by Various Methods of Analysis and Compared to Mean Values of the Three Methods

with no indication of which is more representative of the true K_La'. It is not proposed that agreement necessarily assures the lack of defects, but rather agreement does provide added confidence in the results. Consequently, we believe consideration should be given to a requirement for some degree of agreement between the two methods as criteria for acceptance of the results. As an example, an acceptable range of $(K_La')_D/(K_La')_{LD}$ of 0.95 -1.05 and of $(C^*_\infty)_D/(C^*_\infty)_{LD}$ of 0.975-1.025 might be appropriate.

Conclusions

1. A correction procedure for exit gas composition, which is based on the earlier work of Downing, provides a theoretically sound, uncomplicated method for obtaining clean water K_La values in the non-steady state test. The method appears to acceptably account for the reduced and changing gas phase oxygen concentration and consequent change in saturation value during the progress of the test.
2. The method also, through the use of *in situ* saturation values, appears to acceptably account for submergence and interfacial area distribution, which though substantially constant in any given test may vary significantly under various conditions with the same device and are known to vary widely with different types of aeration equipment. The general applicability of the method is thus enhanced.
3. Some inaccuracies which result from assumptions in the Downing derivation may be reduced by use of a modification which more accurately accounts for gas phase oxygen mole fraction with significant consequent additional complexity. We consider this modified procedure to be more representative of conditions as they exist, and consequently preferred.
4. Either of the above procedures are considered to provide greater accuracy and applicability than other methods in common use which treat C^* as a constant and obtain the value to be used from oxygen solubility tables, corrected for temperature, pressure, and an assumed interfacial area distribution profile.
5. The Downing approach is also considered to provide greater accuracy and applicability than other methods which employ a measured or graphically obtained saturation value, which is treated as a constant in the determination of K_La'.
6. When the saturation value in the log deficit method is obtained by measurement after long periods of aeration, the direct and log deficit methods provide values of apparent C^*, K_La, and OTE which are in very good overall agreement.
7. One, the other, or more likely both methods of analysis are lacking in reproducibility, particularly with respect to apparent K_La and OTE when applied to available data. However, there is some evidence that the methods may yield about the same level of reproducibility.
8. There is some evidence that suggests that good agreement exists in K_La' values obtained by the non-linear least squares and the direct and log deficit methods.
9. There is some evidence that the non-linear least squares method of analysis may provide greater precision than the direct or log deficit methods.
10. The non-steady state test gives no direct information to aid the designer in the selection of the appropriate field DO concentration, which is necessary to compute the available field driving force. In systems and at loadings where DO concentration is substantially uniform, this is of minor consequence. Where the converse is true, this may represent a significant source of error in the application of non-steady state data to field use.

Acknowledgements

This work forms a part of Sanitaire — Water Pollution Control Corporation's continuing research program in aeration. We gratefully acknowledge the opportunity to participate in this program. We would further like to express our appreciation for the comments and guidance offered by Professors Boyle and Baillod; for the data analysis and review by Mr. John Lee; and for encouragement, support, and critical review provided by Mr. Lloyd Ewing.

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Discussion

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The purpose of this discussion is to clarify a few points raised by the authors concerning the subsurface models based on the "true K_La" and "apparent K_La". For a given situation (aerator, basin geometry, water characteristics, temperature, etc.) the two models will give identical values for the oxygen transfer rate since, by definition:

$$dC/dt = K_La' (C^*_\infty - C) = K_La (C^* - C) \quad (1)$$

It is only when adjustments for water characteristics and temperature are applied without distinguishing between the true and apparent values[1] of α and θ that the OTR values predicted by each model will begin to diverge.

In the approach based upon the "true K_La", the impact of gas side oxygen depletion must be considered both in the interpretation of non-steady state test data and in the field application. However, this approach has the advantage of dealing with a real volumetric mass transfer coefficient. Consequently, the alpha and theta factors are defined as ratios of mass transfer coefficients.

The "apparent K_La" approach is simpler in that the impact of gas side oxygen depletion is automatically reflected in the estimate of K_La'. A drawback of this approach is that the alpha and theta factors are not represented as simple ratios of volumetric mass transfer coefficients. By combining the Downing-Boon model as given by Equation 30[1] with the definitions of α and α', it can be shown that:

$$\alpha' = \alpha(\phi_{df}/\phi_d)\,[\phi_d + K_La]/\{(\phi_{df} + (K_La)_f]\} \quad (2)$$

where:

ϕ_{df} = correction number evaluated at dirty water field conditions:

If the problems of scale-up are neglected, i.e., the same gas rates and geometry prevail in the clean and dirty water conditions,

$$\phi_{df}/\phi_d = 1/\beta \quad (3)$$

and:

$$\alpha' = \alpha[(\phi_d + K_La)/(\phi_d + \alpha\beta K_La)] \quad (4)$$

Application of typical values of $\phi_d = 1.5\ \text{min}^{-1}$, $K_La = 0.15\ \text{min}^{-1}$, $\alpha = 0.5$, and $\beta = 0.9$ in Equation 4 indicates that the ratio α'/α would be about 1.057, or that failure to distinguish between α and α' could result in an error of 5.7%.

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[1] For a definition of terms and reference to equation numbers, see: Baillod, C.R., "Review of Oxygen Transfer Model Refinements and Data Interpretation", which appears earlier in these Proceedings.

A similar development shows that the true and apparent values of θ are related by:

(5) $$(\theta')^{\Delta T} = \theta^{\Delta T}(\phi_{df}/\phi_{20})\left[\frac{\phi_{20}+(K_La)_{20}}{\phi_{df}+(K_La)_f}\right]$$

Neglecting scale-up gives:

(6) $$\phi_{df}/\phi_{20} = 1/\tau$$

so that:

(7) $$(\theta'/\theta)^{\Delta T} = [\phi_{20}+(K_La)_{20}]/[\phi_{20}+\theta^{\Delta T}(K_La)_{20}]$$

Again, application of typical values ($\phi = 1.5\ \text{min}^{-1}$, $\tau K_La = 0.15\ \text{min}^{-1}$, $\tau = 1.1$, $\theta = 1.02$) indicates that:

$$(\theta'/\theta)^5 = 0.996$$

or that failure to distinguish between the true and apparent values of θ could account for a 0.5% error when adjusting over 5°C. The net error introduced by failure to distinguish between true and apparent values of both α and θ would, therefore, amount to about 7%. The typical values employed here correspond to an $(OTE)_0$ of roughly 10%. The approximate error of 5% given by this comparison agrees very well with that indicated by the authors' Figure 1 for K_La Downing vs K_La Uncorrected at $(OTE)_{SC}$ of 10%.

However, even for a given sample of wastewater, it is difficult to measure α within an accuracy of 5%. Considering wastewater variability, the accuracy of a "design" value of α might be ±15%. Likewise, the uncertainty involved in values of θ can result in substantial errors apart from the failure to distinguish between θ and θ'. Values of θ or θ' ranging from 1.020 to 1.030 are commonly employed and, over 5°C, this alone would lead to differences of about 5% in the prediction of field transfer rates.

It is important that the modeller and design engineer clearly distinguish between subsurface aeration models based on true and apparent values of K_La, α, and θ. Failure to recognize the differences between the true and apparent values of these parameters can lead to small, but systematic, errors in correcting clean water test results to field conditions.

Appendix A

Four Methods of Data Analysis

	Uncorrected	Mid-Depth Exit Gas	Downing	Modified Downing
C* Used	$(C^*_\infty)_{LD}$	$C^*_{sbT}\left[\frac{0.433 p_a Z_d}{29.4} + \frac{p_a Y_e}{(29.9)(0.42)}\right]$	$(C^*_\infty)_{LD}$	$(C^*_\infty)_{LD}$
K_La'	—	—	LRA†: $\ln(C^*_\infty - C)$ vs t	LRA†: $\ln(C^*_\infty - C)$ vs t
$(dC/dt)_0$	Not Req'd	Not Req'd	$(K_La')(C^*_\infty)$	$(K_La')(C^*_\infty)$
K_La	LRA†: $\ln(C^*_\infty - C)$ vs t	LRA†: $\ln(C^* - C)$ vs t	K_La'/F_0	K_La'/F'_0
$(K_La)_{20}$	$K_La\left[1.02^{(20-T)}\right]$			
$(OTE)_{SC}$	$(K_La)_{20}(C^*)(V/Q_o)$			
C^*_f ‡	$C^*_{\infty 20}\left[\frac{C^*_{sbT}}{C^*_{sb20}} \times \frac{P_f}{29.9}\right]\beta$	$C^*_{sbT}\left[\frac{0.443 p_f Z_d}{29.4} + \frac{p_f Y_e}{(29.9)(0.42)}\right]\beta$	$C^*_{\infty}20\left[\frac{C^*_{sbT}}{C^*_{sb20}} \times \frac{p_f}{29.92}\right]\beta F_{tf}$	$C^*_{\infty}20\left[\frac{C^*_{sbT}}{C^*_{sb20}} \times \frac{p_f}{29.92}\right]\beta F'_{tf}$
$(OTE)_f$	$\alpha K_La(C^*_f - C)(V/Q_o)$			

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†LRA – Linear Regression Analysis.

‡First approximation rather than trial and error used in all cases.

Appendix B

Comparative Results of Various Methods of C* Selection

Summary of Various Runs

Assumed Field Conditions: $C_f = 2$ mg/l $\alpha = 0.8$ $\beta = 0.9$ T = 26°C p = 29.0 in. Hg

Run	C* Selection Method	Q_a/V $\left(\frac{\text{scfm}}{1000\ \text{ft}^3}\right)$	Z_d (ft)	SOTR/V $\left(\frac{\text{lb/day}}{1000\ \text{ft}^3}\right)$	C^*_{20} (mg/l)	D_E	$(K_La)_{20}$ (hr^{-1})	$(OTE)_{SC}$	$(OTE)_f$
34C	Uncorrected	37.0	20.3	167.0	10.46	0.29	11.12	0.187	0.0997
34C	Mid-D. Exit Gas	37.0	20.3	167.0	11.30	0.45	9.10	0.166	0.0942
34C	Downing	37.0	20.3	167.0	10.46	0.29	12.27	0.187	0.1028
34C	Mod. Downing	37.0	20.3	167.0	10.46	0.29	12.05	0.187	0.1023
3F	Uncorrected	59.2	13.0	59.2	9.71	0.24	10.65	0.105	0.0544
3F	Mid-D. Exit Gas	59.2	13.0	59.2	9.71	0.49	8.06	0.086	0.0474
3F	Downing	59.2	13.0	59.2	9.71	0.24	11.27	0.105	0.0552
3F	Mod. Downing	59.2	13.0	59.2	9.71	0.24	11.15	0.105	0.0550
2E	Uncorrected	33.6	21.3	333.0	11.21	0.41	18.81	0.375	0.2042
2E	Mid-D. Exit Gas	33.6	21.3	333.0	10.65	0.31	24.02	0.455	0.2603
2E	Downing	33.6	21.3	333.0	11.21	0.41	23.15	0.375	0.2183
2E	Mod. Downing	33.6	21.3	333.0	11.21	0.41	22.41	0.375	0.2168
1G	Uncorrected	20.8	14.0	37.6	10.17	0.34	2.47	0.0723	0.0388
1G	Mid-D. Exit Gas	20.8	14.0	37.6	10.88	0.53	2.00	0.0627	0.0344
1G	Downing	20.8	14.0	37.6	10.17	0.34	2.56	0.0723	0.0385
1G	Mod. Downing	20.8	14.0	37.6	10.17	0.34	2.54	0.0723	0.0384
10HD	Uncorrected	0.63	10.0	2.93	10.50	0.61	0.186	0.1837	0.0979
10HD	Mid-D. Exit Gas	0.63	10.0	2.93	9.86	0.37	0.222	0.2058	0.1119
10HD	Downing	0.63	10.0	2.93	10.50	0.61	0.205	0.1837	0.1009
10HD	Mod. Downing	0.63	10.0	2.93	10.50	0.61	0.201	0.1837	0.1002
29E	Uncorrected	12.5	14.3	97.0	10.63		6.06	0.2944	0.1575
29E	Mid-D. Exit Gas	12.5	14.3	97.0	9.82		7.05	0.3164	0.1809
29E	Downing	12.5	14.3	97.0	10.63		7.10	0.2944	0.1655
29E	Mod. Downing	12.5	14.3	97.0	10.63		6.92	0.2944	0.1646

Appendix C

Ratios of Other Methods to Modified Downing Method

Run	$(OTE)_{SC}$	$\frac{[(K_La)_{20}]_N}{[(K_La)_{20}]_{MN}}$	$\frac{[(K_La)_{20}]_U}{[(K_La)_{20}]_{MN}}$	$\frac{[(K_La)_{20}]_{MD}}{[(K_La)_{20}]_{MN}}$	$\frac{[(OTE)_f]_N}{[(OTE)_f]_{MN}}$	$\frac{[(OTE)_f]_U}{[(OTE)_f]_{MN}}$	$\frac{[(OTE)_f]_{MD}}{[(OTE)_f]_{MN}}$
34C	0.1873	1.018	0.923	0.755	1.005	0.975	0.921
3F	0.1047	1.011	0.955	0.723	1.004	0.989	0.862
2E	0.3750	1.033	0.839	1.072	1.007	0.942	1.201
1G	0.723	1.008	0.972	0.787	1.003	1.010	0.896
10HD	0.1840	1.020	0.925	1.105	1.007	0.977	1.117
29E	0.294	1.026	0.876	1.019	1.005	0.957	1.099

Subscripts: N - Downing; MN — Modified Downing; U — Uncorrected; MD — Mid-depth exit gas.

Measurement of Alpha and Beta Factors

18

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Introduction

Many parameters influence oxygen transfer in biological wastewater treatment processes. Specifically, wastewater constituents, liquid temperature, DO content (driving force), type of aeration device, basin power level, and basin geometry, to mention a few, affect oxygen transfer. Since these parameters are usually different for each design application, and in order to provide for consistency in the evaluation of aeration devices, aeration equipment manufacturers rate their equipment at standard conditions (5). Standard conditions are defined as clean or "tap" water at 20°C liquid temperature, 760 mm Hg barometric pressure, and maximum driving force (zero DO).

Design engineers compute oxygen transfer requirements at waste or field conditions, and, therefore, must properly correct for the difference in oxygen transfer rates between field and standard conditions. An overall correction factor can be used to adjust from field to standard conditions as follows:

$$(1) \qquad \mathrm{SOTR} = \mathrm{OTR}/\alpha[(\beta C^*_y - C)/C^*_y]\theta^{(T-20)}$$

This correction factor accounts for the following specific differences between standard and field conditions:

—wastewater constituents

—temperature

—driving force

—barometric pressure

Of the above, wastewater constituents and temperature influence both the overall oxygen transfer rate coefficient, K_La, and DO saturation concentration, C* (and subsequently driving force).

Temperature, barometric pressure, and operating DO level values for Equation 1 are easily determined. The effects of wastewater constituents as expressed by the alpha and beta factors and the temperature correction factor are not determined as easily. The sensitivity and significance of these three factors are discussed in this paper. Current measurement practices together with results of various studies are reviewed. Recommendations for development of standardized alpha and beta measurement procedures are presented.

Significance of Variation

The alpha factor, α, is the ratio of the overall oxygen transfer rate coefficient in wastewater to that in clean or "tap" water and is expressed as:

$$(2) \qquad \alpha = K_La\ (\text{wastewater})/K_La\ (\text{clean water})$$

Alpha is affected by wastewater constituents such as soluble BOD, COD, suspended solids concentration, surface tension of the wastewater, and temperature and by system parameters such as type of aeration device, power level, and basin configuration. In general, the influence of these variables can be reduced substantially by conducting the alpha measurement at field design conditions.

The beta factor, β , is the ratio of the system DO saturation concentration in wastewater to that in clean water and is expressed as:

$$(3) \qquad \beta = C^*\ (\text{wastewater})/C^*\ (\text{clean water})$$

Beta is influenced by wastewater constituents such as salts, organics, and dissolved gases and by barometric pressure and temperature. Conducting measurements at equal barometric pressure and at field design temperature will reduce variations to those caused by wastewater constituents alone.

Typical response curves for reaeration of clean water and wastewater are presented in Figure 1. Each curve has a unique overall oxygen transfer rate coefficient, K_La, and DO saturation concentration, C*. Determination of alpha is possible by one or more of several methods of analysis (18). Beta is determined by direct measurement. In the example presented in Figure 1, alpha equals 0.7 and beta equals 0.9. These are typical values for a wide variety of industrial wastewaters and for municipal wastewater. The significance of departure from these values is illustrated in Figure 2.

The magnitude of error in selection of alpha and beta values can be substantial. Reduction in an alpha factor from 0.7 to 0.5 will result in an oxygen deficit of 40% between requirements at field versus standard conditions when all other factors are held constant. Similarly, an alpha factor of 0.9 will result in an excess standard oxygen value of 22%. When both alpha and beta vary from a design value, the effect is compounded. A comparison of this effect is presented below for a design alpha = 0.7 and beta = 0.9. Equation 1 is used to compute the percent change in oxygen required at standard conditions.

Variance in α&β	% Variance	% Change in O_2 Required α only	 α& β
+0.1	+14(α),+11(β)	+12 (excess)	+24
-0.1	−14(α),−11(β)	−17 (deficit)	−36

As the variance in actual versus design alpha and beta factors increases, differences between oxygen required at field versus standard conditions also increases, with the deficit effect being more pronounced. This effect is illustrated by the two curves in Figure 2. Based upon the relationship between alpha and beta factors and resulting oxygen requirements at field versus standard conditions, it is no wonder that aeration systems are frequently over or underdesigned.

Variation in the alpha factor is common, as illustrated by the typical laboratory data for various industrial wastes presented in Table 1 (13). Not only do alpha factors vary more than 100% for similar raw wastes, but as biooxidation proceeds and contaminants are destroyed, the alpha factor approaches that in clean water. Alpha factors for municipal wastewater range from 0.7 to 0.9, whereas alpha values for combined and industrial wastewater become more variable as the wastewater becomes more complex. Not only do alpha factors vary from industry to industry, but also from plant to plant within an industry. It is not uncommon to observe changes in the alpha factor from week-to-week, day-to-day, and even hour-to-hour, depending upon factors such as production scheduling and rain water dilution. The author has observed changes in alpha from 0.8 to 0.5 in a brewery waste over a 4-hr period. As discussed earlier, these variations in alpha cause even greater variations in oxygen transfer.

Temperature affects both equilibrium values for oxygen concentration and the rate at which transfer occurs due to changes in kinematic viscosity, which in turn affects oxygen diffusion into the liquid. The effect of temperature on the overall oxygen transfer rate coefficient is usually expressed as follows:

$$(K_La)_T = (K_La)_{20}\,\theta^{(T-20)} \tag{4}$$

The temperature correction factor has been reported to vary from less than 1.01 to more than 1.05. A comparison of four temperature correction factor references is presented in Figure 3. Eckenfelder's theta value of 1.02 (13) was developed from experimental data and by comparisons of his work with that of Streeter (39), Howland (19), and Wilke (44). O'Connor's temperature correction equation is based on changes in viscosity at the respective temperature (34). Shell suggests that two temperature correction factors be used, a value of 1.03 for temperatures less than 20°C and a value of 1.01 for temperatures greater than 20°C (4). A theta value of 1.024 is considered realistic by many (32).

Data analysis by the author of more than 180 full-scale reaeration tests conducted in water between 14°C and 26°C revealed temperature correction factors between 1.024 and 1.026. A data summary is presented in Table 2.

As can be seen from the curves in Figure 3, temperature correction factors vary by approximately 15% between 10°C and 30°C. Most field conditions are within this temperature range. Caution is advised, however, when correcting field data to standard conditions outside the 10 to 30°C range where variances become greater. Also, the presence of surface active agents and other waste constituents may affect the temperature correction factor.

Measurement and Reliability

A great deal of literature has been published regarding measurement of alpha and beta factors and the difficulties involved both with the apparatus used and liquids being tested. The type of aeration device, mixing intensity, and wastewater constituents present are only three parameters that affect the outcome of the alpha and beta measurement. Researchers are not in agreement concerning such questions as the effect of suspended solids on alpha, mixing level versus K_La, surfactants effect, type of aeration device to be used, and probe correction factors for measurement of beta, to mention a few. The literature contains more controversy regarding these questions than clarification. There is a great need for both fundamental and applied research in the areas of alpha and beta measurement.

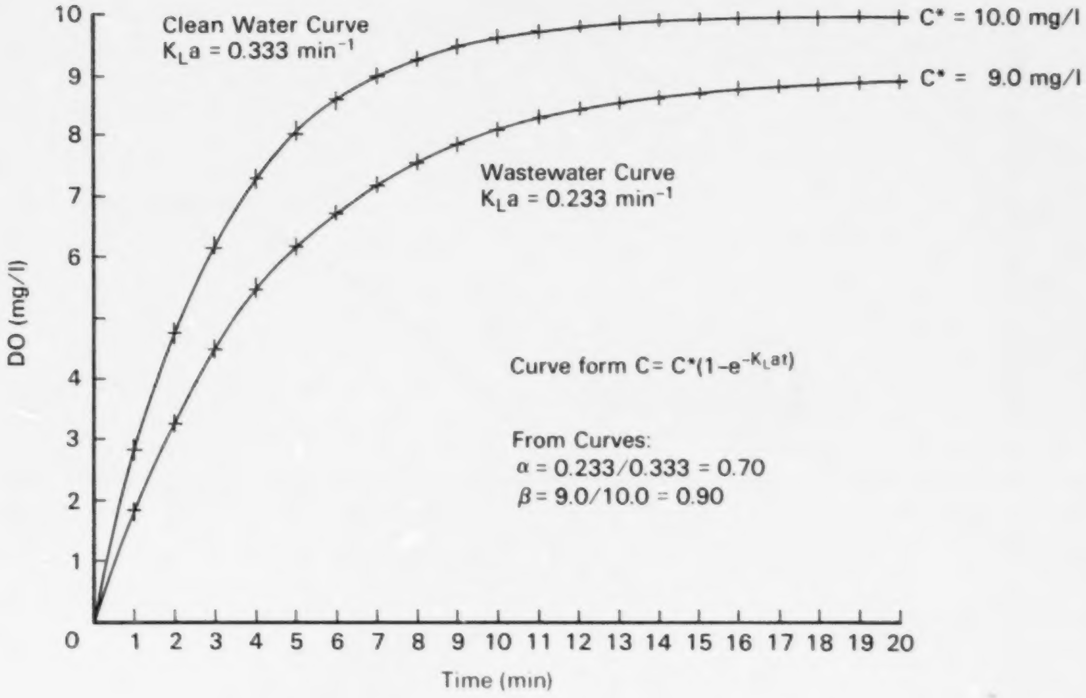
Alpha Measurement

Test Apparatus and Aeration Device

Bass and Shell (4), Stukenberg, Wahbeh, and McKinney (40), and other investigators (3) (8) (15) (16) (17) (29) (35) have described test apparatus used to conduct alpha measurements. Three of these test devices are shown in Figures 4, 5, and 6. The bench-scale aeration tank shown in Figure 4 contains both a submerged turbine agitator and an air sparger resembling a submerged aerator (40). The tank has a 10-liter liquid capacity. The diffused aeration equipment shown in Figure 5 employs a fritted cylinder diffuser in a 16-liter aeration tank (4). Apparatus resembling a surface aerator is shown in Figure 6 (4). The tank has a 28-liter capacity, and mixing intensity is controlled by varying agitator motor speed. The latter two aeration devices have been used to conduct comparative alpha measurements.

Otoski (35) and Marotte (29) have used alpha test apparatus employing more than one type of aeration device in their respective studies, as follows:

Figure 1. Response Curves Illustrating Alpha/Beta Effect



Curve form $C = C^*(1 - e^{-K_Lat})$

From Curves:
$\alpha = 0.233/0.333 = 0.70$
$\beta = 9.0/10.0 = 0.90$

Table 1. Summary of Oxygen Transfer Characteristics of Some Industrial Wastes and Their Bio-Oxidation Effluents (13).

Waste	BOD (mg/l)		α	
	Raw	Effluent	Raw	Effluent
*Paper repulping	187	50	0.68	0.77
*Semi-chemical machine backwater	1872	—	1.40	—
Mixed kraft mill	150-300	37-48	0.48-0.86	0.70-1.11
†Pulp and paper (bleach plant)	250‡	30‡	0.83-1.98	0.86-1.0
†Pulp and paper (pulp mill)	205‡	—	0.66-1.29	—
Pharmaceutical	4500	380	1.65-2.15	0.73-0.83
Domestic sewage (fresh)	180	9	0.82	0.98
Synthetic fiber	5400	585	1.88-3.23	1.04-2.65
Board mill	660‡	—	0.53-0.64	—

*Paper repulping and semi-chemical wastes mixed prior to bio-oxidation.

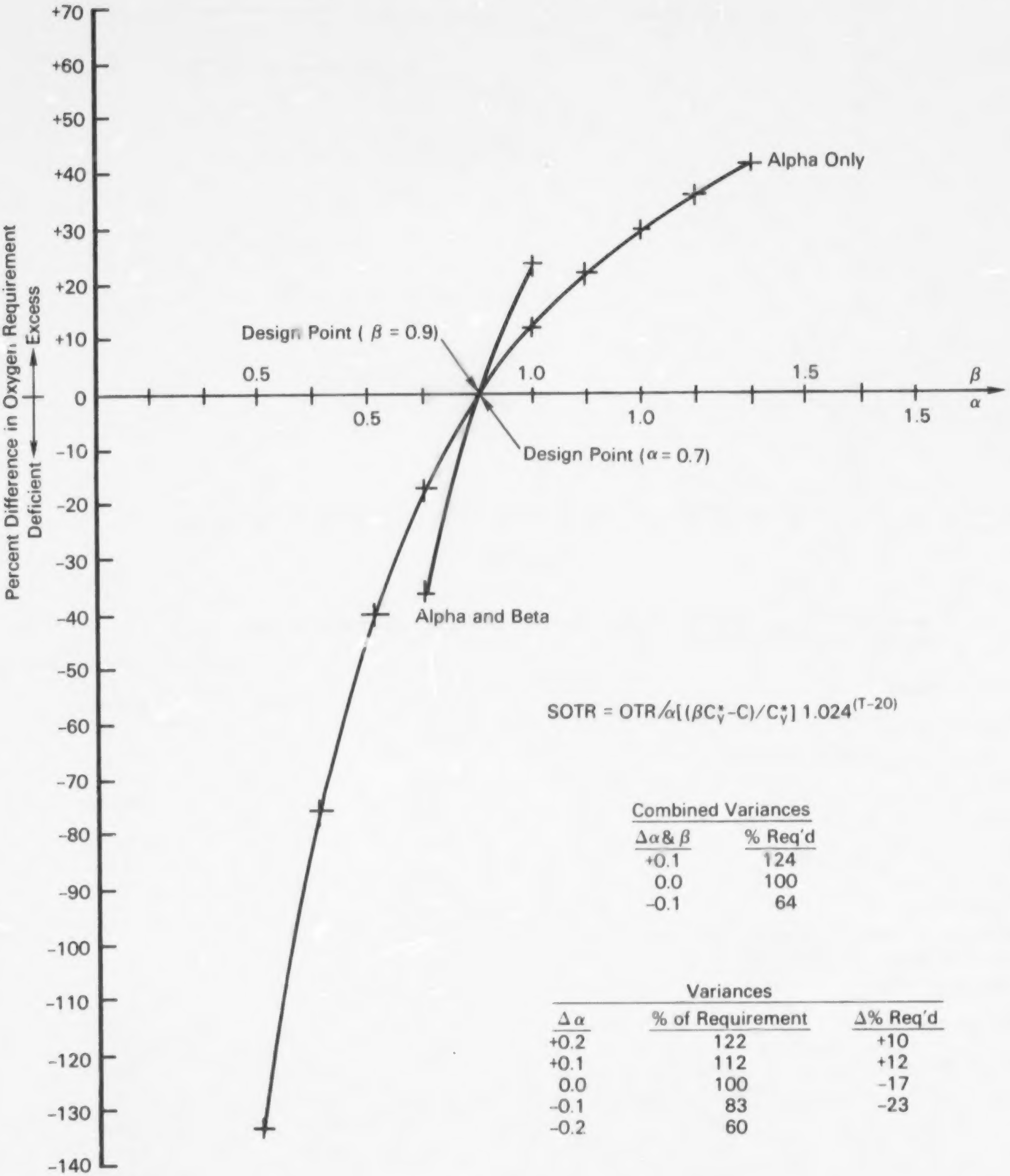
†Bleach plant and pulp mill wastes mixed prior to bio-oxidation.

‡Average values.

Table 2. Temperature Correction Factor Determinations

Tank Water Depth (ft)	Air Flow per Aerator (scfm)	No. of Tests	Temperature Range (°C)	Theta θ
20	42-45	18	14-25	1.024
10	19-22	46	15-26	1.025
10	28-30	74	15-25	1.024
10	42-48	43	15.25	1.026

Figure 2. Relationship Between Alpha/Beta and Oxygen Requirements



$$SOTR = OTR/\alpha[(\beta C^*_\gamma - C)/C^*_\gamma]\,1.024^{(T-20)}$$

Combined Variances

$\Delta\alpha$ & β	% Req'd
+0.1	124
0.0	100
-0.1	64

Variances

$\Delta\alpha$	% of Requirement	Δ% Req'd
+0.2	122	+10
+0.1	112	+12
0.0	100	-17
-0.1	83	-23
-0.2	60	

Figure 3. Effect of Temperature on Oxygen Transfer Rate, $(K_La)_T = (K_La)_{20}\,\theta^{(T-20)}$

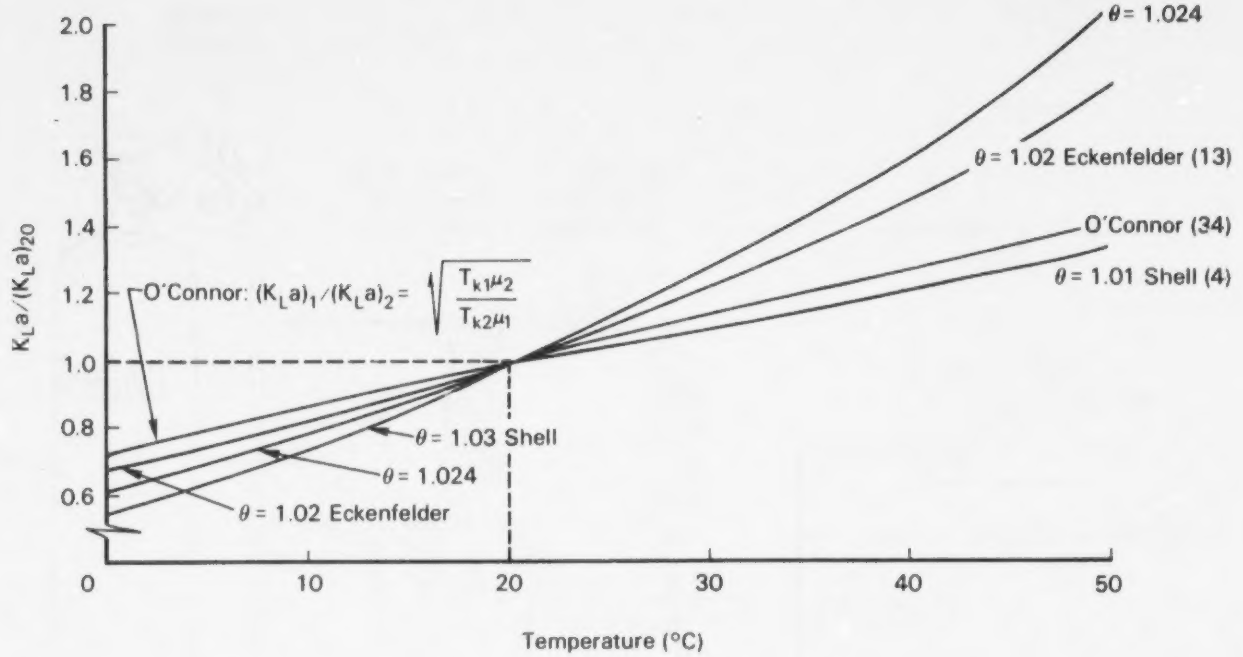
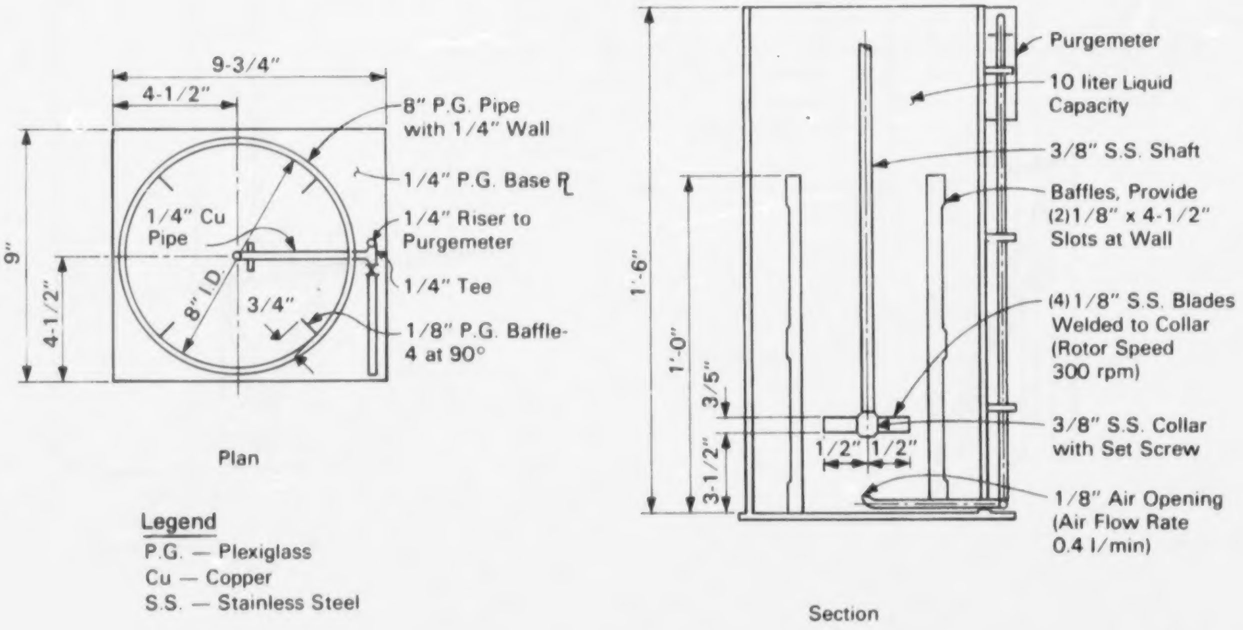


Figure 4. Bench-Scale Aeration Tank (40)



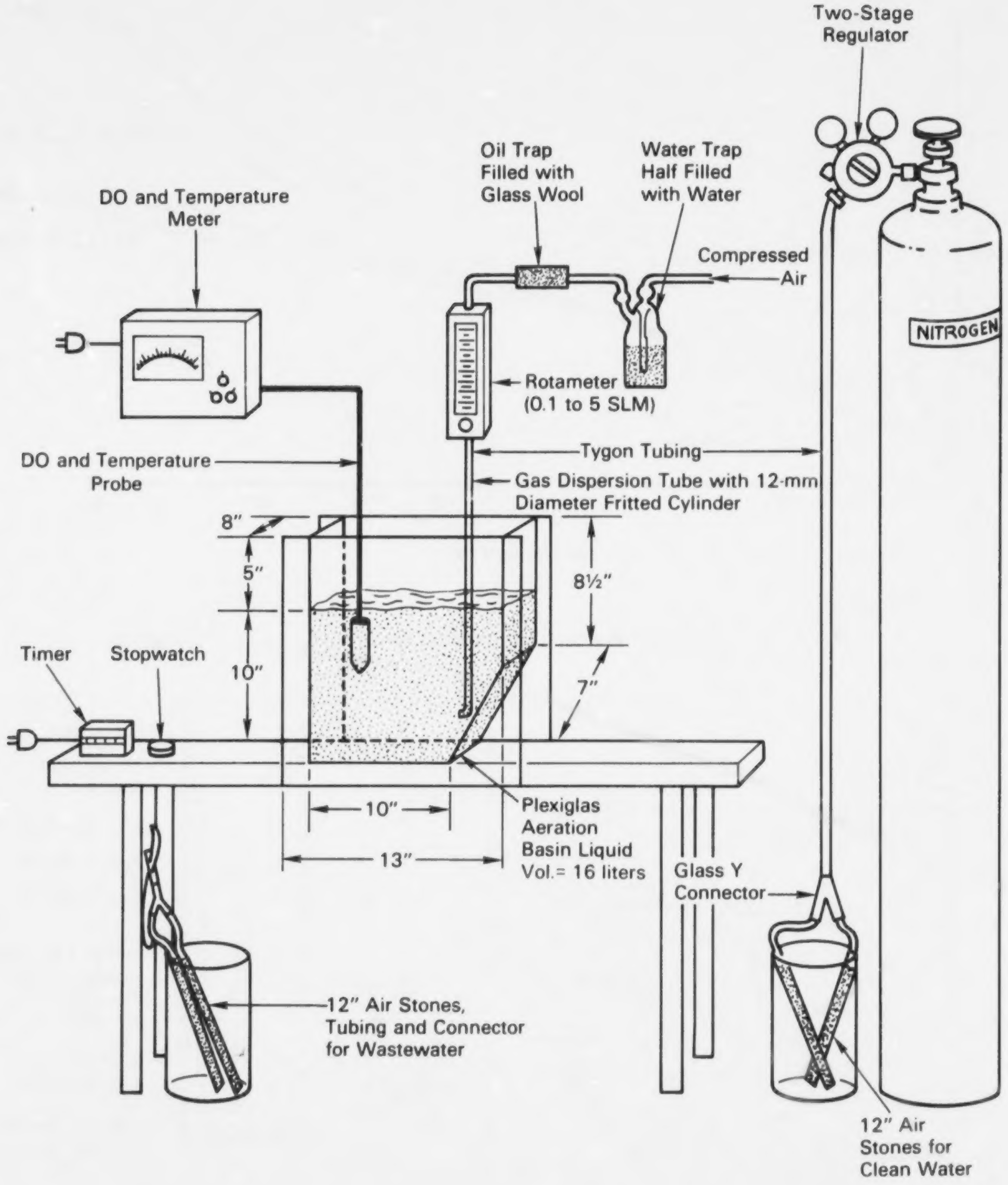
9-3/4"
4-1/2"
8" P.G. Pipe with 1/4" Wall
1/4" P.G. Base PL
1/4" Cu Pipe
1/4" Riser to Purgemeter
1/4" Tee
9"
4-1/2"
8" I.D.
3/4"
1/8" P.G. Baffle-4 at 90°
Purgemeter
10 liter Liquid Capacity
3/8" S.S. Shaft
Baffles, Provide (2)1/8" x 4-1/2" Slots at Wall
(4) 1/8" S.S. Blades Welded to Collar (Rotor Speed 300 rpm)
3/8" S.S. Collar with Set Screw
1/8" Air Opening (Air Flow Rate 0.4 l/min)
1'-6"
1'-0"
3/5"
3-1/2"
1/2"
1/2"

Plan

Legend
P.G. — Plexiglass
Cu — Copper
S.S. — Stainless Steel

Section

Figure 5. Schematic Diagram of Diffused Aeration Alpha Apparatus (4)


Two-Stage Regulator
Oil Trap Filled with Glass Wool
Water Trap Half Filled with Water
DO and Temperature Meter
Compressed Air
NITROGEN
Rotameter (0.1 to 5 SLM)
Tygon Tubing
DO and Temperature Probe
Gas Dispersion Tube with 12-mm Diameter Fritted Cylinder
8"
5"
8½"
10"
7"
Timer
Stopwatch
10"
13"
Plexiglas Aeration Basin Liquid Vol.= 16 liters
Glass Y Connector
12" Air Stones, Tubing and Connector for Wastewater
12" Air Stones for Clean Water

Ostoski	Marotte
—Surface agitator with lower mixing impeller	—Surface agitator
	—Static Mixer
—Diffuser stones	—Submerged agitator with air sparger
—Static Mixer	—Diffuser

Both investigators have observed variations in alpha with an aeration device over a wide range in K_La. Similar observations were made by Bass and Shell (4) from alpha tests conducted on a complex industrial wastewater. From these studies, it appears that variations of up to 40% can occur in alpha measurements simply as a result of using different aeration devices.

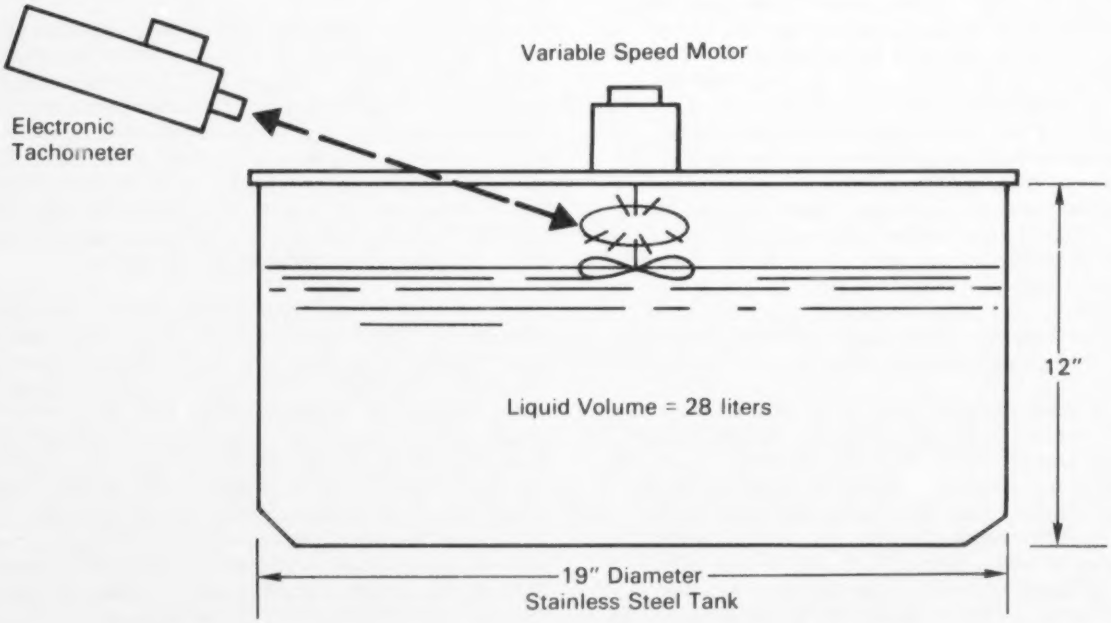
Similitude of aeration devices seems to be a reasonable approach for minimizing measurement effects of the aeration device. Kalinske (23) and others (8)(33) state that the same type of aeration device used in the field installation should be used in the alpha test vessel. The author has had good success using an aeration device scaled down from the prototype aerator for alpha measurements.

Turbulence

Alpha measurement is also affected by the mixing level or turbulence in the test vessel. WPCF Manual No. 5 (1) cautions that extreme care must be employed in the interpretation of bench-scale alpha data, and that the pattern of turbulence has an influence on alpha which may be different than from full-scale aeration tanks. Otoski (35) found a direct relationship between turbulence and alpha value for all three aeration devices studied. Stukenberg *et al.* (40), Weis and Lad (43), and Bass and Shell (4) recommend that alpha tests be conducted so that the alpha test vessel K_La approximates the field value to minimize the turbulence factor. It is believed that with the same K_La, the two systems will be similar enough in mixing characteristics to yield the same alpha factors within normal experimental differences.

Recent studies, conducted to determine the effect alpha test vessel liquid depth has on alpha (29) (45) and to compare bench- with full-scale alpha measurements (35), indicate that matching test vessel and field K_La values may not represent equivalent mixing conditions for submerged aeration devices. K_La is a function of depth in submerged aeration; therefore, an alpha test vessel 2 ft in depth requires greater air flow per unit volume than does a prototype aeration system 20 ft in depth with an equivalent K_La. Comparative tests between a 3 ft deep alpha test vessel and a 10 ft deep full-sized system, using scaled down and full sized aeration devices, revealed the following relationship between air flow rate per unit volume and K_La:

Figure 6. Schematic Diagram of Surface Aeration Alpha Appratus (4)



Bench-Scale Air Flow Rate*	Bench-Scale K_La, 3 ft Liquid Depth	Full-Scale K_La, 10 ft Liquid Depth
25 scfh**	0.08 min^{-1}	0.24 min^{-1}
40 scfh**	0.13 min^{-1}	0.33 min^{-1}

*Air flow rate per unit volume same for both systems.
**Standard ft^3/hr.

For equal air flow per unit volume, K_La in the full-scale aeration system was 2.5 to 3 times greater than in the bench-scale unit. The additional depth in the full-scale system accounted for the larger K_La value.

Zlokarnik (45) recommends that alpha test vessels, using submerged aeration devices, have at least a 10-ft liquid depth to provide for adequate bubble formation and coalescense properties characteristic of a full-scale aeration system. Both Marotte (29) and Zlokarnik (45) have observed variations in alpha values with test vessel liquid depth. In light of the effects that turbulence has on alpha measurement, determination of alpha at several aeration intensities should be considered prior to selecting an appropriate value(s) for design purposes.

Suspended Solids

There seems to be little or no concensus concerning the effect that mixed liquor suspended solids (MLSS) has on alpha measurement. Stukenberg *et al.* (40) explain two ways in which oxygen transfer can be affected by MLSS: by changing viscosity and by releasing soluble organics. It is their belief that the MLSS concentration under aeration is not high enough to adversely affect viscosity. On the other hand, soluble organics released by the active microbial mass in the aeration tank could affect oxygen transfer by releasing soluble surface active materials in the presence of an oxygen-deficient environment. As long as the cell mass is aerobic, organics should be consumed and there should be no affect upon alpha. The authors further state that alpha can be determined on the final effluent or on the supernatant from the settled mixed liquor, rather than the mixed liquor under aeration. Obviously, if the activated sludge is allowed to go anaerobic during testing, release of some organics could reduce the alpha factor below the actual value in the system. However, since alpha approaches unity in proportion to purification of the wastewater (13) (26) (40), conducting alpha measurements on the final effluent may be on questionable value.

Bass and Shell (4) recommend that alpha determinations be preformed on waste that contains the same approximate type and concentration of biological solids as will be present in the full-scale system. Supportive data indicate that the presence of biological solids has a significant detrimental effect on the oxygen transfer rate in samples of waste containing solids, versus samples which had been filtered prior to testing. Casey and Karmo (22) found that activated sludge solids in concentrations of 2,000 mg/l affected the rate of oxygen transfer; whereas, non-flocculated suspensions of polyvinyl chloride (PVC) powder and fine and coarse dried peat had a negligible effect.

Jackson *et al.* (6) report average alpha values of 0.63 for groundwood paper mill wastewater mixed liquor both in bench- and in full-scale tests. They indicate, however, that higher alpha values were obtained with the clear liquor (cells separated) and attribute the difference in part to the fiber content of the wastewater.

Damhaug and Balmer (9) concluded that determination of the alpha factor on activated sludge supernatant or secondary effluent may give misleading results. They summarized literature on the effect of MLSS by stating that the primary effect of suspended solids on K_La is a reduction of liquid hydrodynamics caused by an increase in the liquid viscosity and consequently a reduction in the K_La value. Conversely, Matson and Bennett (30) discussed a positive effect of suspended solids on alpha. They presented findings that microorganisms can increase oxygen transfer rates by disrupting the liquid surface film surrounding the gas bubble.

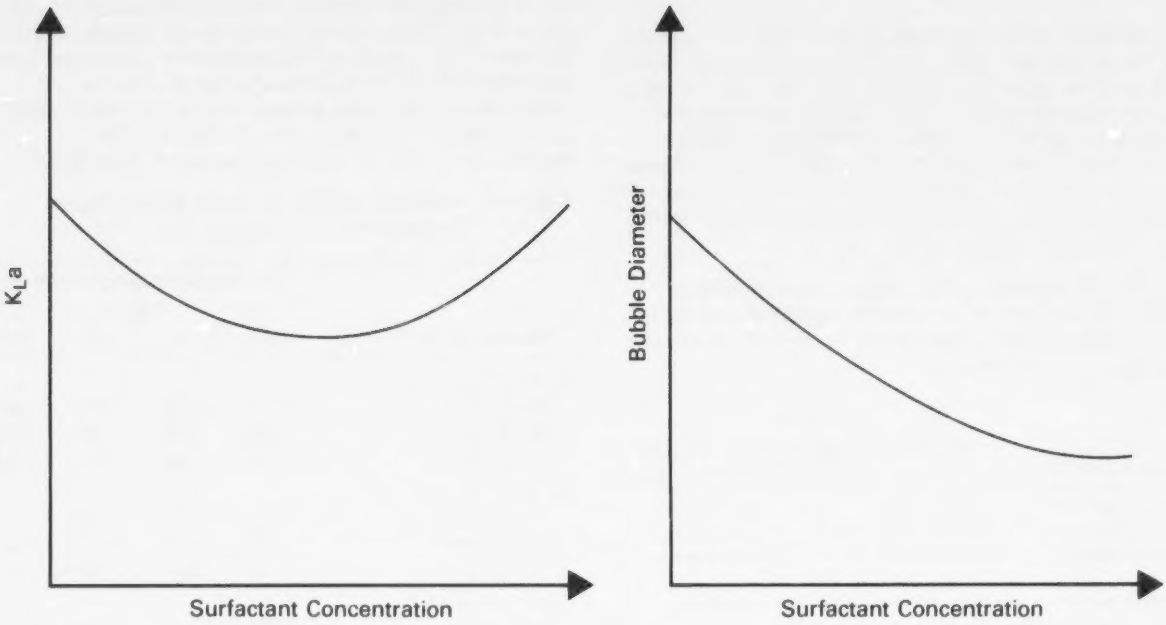
Surface Active Agents

Aeration tank alpha values are dependent on the exact level of bio-oxidation of the organic matter and detergents present. As previously mentioned, partial decomposition of organics produces various organic acids that affect the alpha value as do any other surface active agents present. The "surface active agent effect" is another area where basic and applied research is required.

The presence of materials such as proteins, detergents, oils, and other surface active agents affect film thickness and surface tension. Changes in film characteristics due to dissolved organics tend to decrease oxygen transfer. Aiba and Toda (2) suggested that the effect of surface active agents is to make the interface more quiescent, and, therefore, to decrease the rate of surface renewal. Experiments with pure surfactants and other organics (2) (10) (11) (12) (27) (28) (31) (35) (41) have typically resulted in curves such as those shown in Figure 7.

Eckenfelder and Ford (15) postulated the relationship between the alpha factor and the degree of turbulence generated by the aerator at a given surfactant concentration. The mechanism of interference is shown graphically in Figure 8. Under quiescent conditions, the principle resistance to oxygen transfer is diffusion through the body of liquid so that the establishment of an interfacial film has little effect on the total transfer. At intermediate levels of turbulence, complete mixing is achieved throughout the liquid volume and the interfacial resistance of the absorbed surfactant reduces the transfer rate. At high mixing levels, the interfacial renewal is so great that no interfacial barrier is permitted to be established and the value of alpha is again approximately unity.

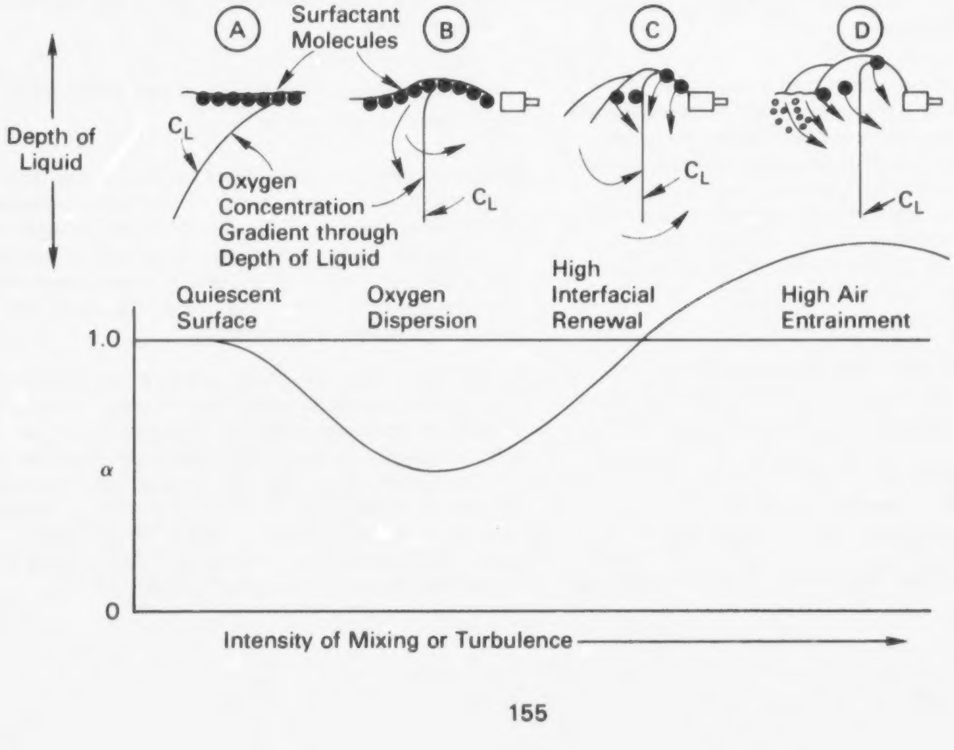
Figure 7. Effect of Surfactant Concentration on Mass Transfer Characteristics



(a)

(b)

Figure 8. Relationship Between the Alpha Factor and the Degree of Turbulence at a Given Surface Active Agent Concentration (15)


Ⓐ
Surfactant Molecules
Ⓑ
Ⓒ
Ⓓ
Depth of Liquid
C_L
Oxygen Concentration Gradient through Depth of Liquid
C_L
C_L
C_L
Quiescent Surface
Oxygen Dispersion
High Interfacial Renewal
High Air Entrainment
1.0
α
0
Intensity of Mixing or Turbulence

Higher degrees of turbulence result in entrainment of fine air bubbles which increase the interfacial area and alpha to a value greater than unity.

Since surface, submerged turbine, and diffused aeration all produce different types of mixing regimes with respect to the surface active agent effect, it is not unreasonable to expect that different values of alpha might result for the same liquid being aerated by different aeration devices. Furthermore, bench- (35) and full-scale surface active agent studies, reported upon here for the first time, indicate that alpha varies significantly with the type of diffused aeration device used.

From Otoski's research (35), it was determined that distinct relationships exist between aeration device, surfactant concentration, and mixing. These relationships are summarized as follows:

	Effect on Alpha as a Function of Increasing:	
Aeration Device	(a) Mixing Level	(b) Surfactant Conc.
Surface agitator	α decreases	α decreases
Static mixer	α increases	α increases
Diffuser stone	α increases	α decreases

The studies were conducted in a 100-liter capacity bench-scale aeration tank using the three aeration devices previously described, three mixing levels, and three levels of surfactant concentration. A pure anionic surface active agent (Richonate 40B — sodium dodecylbenzene sulphonate $[C_{12}H_{25}C_6SO_3Na]$) was used in concentrations of 5, 10, and 20 mg/l. A summary of alpha factors is presented in Table 3 for each aeration device. Similar results have been reported by Marotte (29) for pulp mill wastewater.

Similar full-scale studies, conducted by the author, provided results which were in good agreement with the bench-scale work. The full-scale test facility has been described elsewhere (18). The same surface active agent was used during the testing, aerators were full-sized prototypes, and the volume of water tested was 87,000 gal. Mixing intensities were varied from less than 10 scfm/1,000 ft^3 to over 190 scfm/1,000 ft^3 of aeration volume. Alpha values ranged from unity to over 1.2, averaging 1.1 for all tests.

Generally, alpha increased with mixing intensity as would be expected. Surfactant concentration had little influence on relative oxygen transfer.

A second series of tests were conducted using a coarse bubble sparger aeration system. The type and quantity of surfactant, aeration volume, and mixing intensities remained unchanged. The results of these tests were different; the alpha value averaged approximately 0.7 to 0.8 rather than 1.1 for the static aerators. Alpha values did increase with mixing intensity, however.

Other investigators (37) (42) have conducted full-scale alpha tests using surface active agents. Schmit *et al.* (37) described results of full-scale tests conducted with approximately 20 ft of submergence over broad band coarse bubble diffusers at two mixing intensities (25.4 scfm/1,000 ft^3 and 92.5 scfm/1,000 ft^3). The aeration tank volume was approximately 19,000 gal.

Table 3. Summary of Bench-Scale Alpha Factor Surfactant Test Results (35)

	Surfactant Concentration, mg/l			
Mixing Level[1]	**0**[2]	**5**	**10**	**20**
Static Mixer				
25 SCFH[3]	1.00	0.98	1.00	0.93
40 SCFH[3]	1.00	0.97	1.00	1.06
55 SCFH[3]	1.00	1.09	1.05	1.19
Diffuser Stone				
5 SCFH[3]	1.00	0.85	0.79	—
10 SCFH[3]	1.00	0.91	0.93	—
15 SCFH[3]	1.00	0.91	0.86	—
Surface Agitator				
370 rpm[5]	1.00	0.94	1.00	1.02
440 rpm[5]	1.00	1.05	0.91	0.97
510 rpm[5]	1.00	0.91	0.88	0.79

[1] Mixing levels were considered equivalent in all devices when K_La values in clean water were equal.

[2] K_La values approximately equal in all devices at each mixing level.

[3] Standard ft^3 air/hr.

[4] Tests not conducted due to excessive foaming.

[5] Revolutions per minute.

The type and concentration of surface active agent used in the tests was not reported. Alpha values ranged from 0.65 at the low mixing level to 0.75 at the high mixing level. Bench-scale alpha tests conducted on the same water did not yield similar results. These tests indicated that the presence of the surface active agent had no effect on the bench-scale alpha.

British government research on fine bubble dome difdiffusers (42) indicates that water with a 5 mg/l concentration of detergent produced an alpha value of less than 0.5; whereas, the alpha value in mixed liquor was reported to be about 0.6, but ranged from 0.3 to 0.8 depending upon aeration time. The mixing intensity was approximately 12.5 scfm/1,000 ft^3 at the 20-ft water depth condition. A summary of the three full-scale alpha test results is contained in Table 4.

Table 4. Summary of Full-Scale Alpha Factor Surfactant Test Results

Aeration Device Tested	Aeration Tank Volume (gal)	Tank Depth (ft)	Mixing Level (scfm/1,000 ft^3)	Alpha Factor α
Static Aerator	87,000	10.0	10-190	1.0-1.1
Coarse Bubble Sparger	87,000	10.0	80-190	0.7-0.8
Coarse Bubble Broad Band (37)	19,000	22.5	25-92	0.65-0.75
Fine Bubble (42)	—	5-25	10-30	0.4-0.5

Kalinske (24) reported that at least one large consulting firm considers the non-steady state test using clean water as the only reliable way to evaluate and compare aerator performance as a result of the variable nature of the alpha value. This same consulting firm (6) does require, however, that a surface active agent be present in the clean water tests to approximate the "detergent effect" wastewater has on the aeration device. The alpha values in Table 4 seem to indicate this effect and support justification for use of a surface active agent in clean water tests. Schmit *et al.* (37) indicate that this approach is reasonable.

This author, however, sees no benefit in rating aerator performance with surface active agents present. The only accomplishment would be to introduce another variable to an already too-confused technical area. The combination of types and concentrations of available surface active agents are almost infinite. Another factor to be considered is the reduction in surfactant concentration as an aeration test proceeds. Figure 9 contains a plot of surface tension versus reaeration time for a clean water test in which 5 mg/l of Richonate 40B was added prior to the test. The surface tension dropped from 72 dynes/cm at the beginning of the test before adding the chemical to a low value of 53 dynes/cm 5 min after the test had begun. Surface tension increased to about 60 dynes/cm after 20 min of aeration and remained at that level throughout the remainder of the test. Other test conditions have yielded different results.

Beta Measurement

The beta factor has been defined as the ratio of the dissolved oxygen saturation concentration for wastewater at field conditions to the saturation value for clean or tap water at field conditions. Beta has been commonly referred to as the salinity correction factor because dissolved salts reduce oxygen solubility in wastewater. In addition, dissolved organics and gases in the wastewater can reduce oxygen solubility as well. Unfortunately, these constituents also affect the measurement of DO.

Several methods are available for measurement of DO. The various methods can be divided into three principal categories: 1) laboratory methods, 2) electrochemical analysis, and 3) membrane electrode methods. The most popular and most widely used laboratory method is the Winkler Method (38). This method is used as a standard for calibration and for determining DO content in relatively clean waters. Unfortunately, the Winkler Method is not suitable for use with complex or colored wastewaters due to possible test interferences. (For example, there may be oxidizing or reducing agents present in the sample that may either liberate or reduce iodine which will introduce error). The electrochemical methods are not suitable for measurement in wastewater because of interferences and electrode contamination.

The most popular methods of DO measurement are the membrane electrode methods (36). The success of these methods is due to the isolation of the electrodes and electrolyte from the sample by means of a semi-permeable membrane. There are two types of membrane electrodes: 1) the galvanic membrane electrode and 2) the amperometric membrane electrode. The galvanic type relies upon the development of a potential between two dissimilar metal electrodes when contact is made with oxygen in the sample to be analyzed. The cell generally consists of a lead anode and a platinum or silver cathode. As oxygen passes through the membrane at a rate proportional to its *partial pressure*, a potential develops between the anode and cathode. The resulting current flow is proportional to the oxygen present in the reaction.

The amperometric membrane technique is probably the most widely used method for measurement in wastewater. The electrodes (usually a gold cathode and a silver anode) are driven by an externally applied polarizing voltage. The current flow between the electrodes is directly proportional to the amount of oxygen present in the test solution.

Bass and Shell (4) contend that probes will yield false saturation readings because they measure percent saturation and not actual DO concentration, and, therefore, cannot be used for beta measurement.
The example is given that if a probe is calibrated to read saturation at 9.2 mg/l, it will read 9.2 mg/l when placed in any saturated solution. This is not entirely correct. Probes measure oxygen partial pressue. Inside the membrane, next to the cathode, the oxygen pressure is

zero because oxygen is consumed as it reaches the cathode. Outside the membrane, the oxygen pressure is the ambient pressure to be measured. The flow of oxygen through the membrane is directly proportional to the differential oxygen pressure across the membrane, and since the oxygen pressure is zero on the cathode side of the membrane, the flow is proportional to the absolute oxygen pressure on the outside of the membrane. The current resulting from the chemical reaction is in direct stiochiometric relation to the amout of oxygen being consumed (20) (21).

The apparent false saturation measurement can be corrected on some DO analyzers (21) by making a salinity correction adjustment to the measurement obtained in wastes with high concentrations of dissolved salts. This correction is to compensate for salinity effects on the membrane which change the true rate at which oxygen passes through the membrane and, therefore, the oxygen partial pressure.

The effects of various constituents on oxygen saturation measurements using two types of probes and the Winkler analysis are summarized in Table 5. In some cases, the manufacturer's correction procedures agree with the Winkler Method (NaCl and Na_2SO_4 solutions). In other cases, there is no agreement and even the Winkler Method results are meaningless or questionable. Because of these interferences, Bass and Shell (4) recommend an alternate method of determining beta by computing the ratio of the saturation concentration for tap water at field temperature, pressure, and salt concentration to the saturation for tap water at field temperature, pressure, and zero salt concentration.

Dissolved organics and dissolved gases can also interfere with the rate at which oxygen passes through the membrane. When using a probe to measure DO, one must be aware of the sensitivities of the instrument to the waste constituents present in the sample. The probe manufacturer's literature should be referred to prior to calibration and making measurements. If the literature does not contain information on interferences, then the oxygen analyzer manufacturer should be consulted.

Considerations for a Standard

The complete lack of concensus regarding the significance and influence of the factors affecting alpha and beta determinations presented in this paper demonstrates the need for standardized procedures for determination of alpha and beta factors. Even with standardized procedures, however, the factors may not be valid at the worst, or accurate enough for performance test evaluation at best. Keegan and Busch (25) judged the determination of alpha as neither a valid nor effective procedure. The process Equipment Manufacturer's

Figure 9. Change in Surface Tension During Reaeration Testing with Surfactants Present

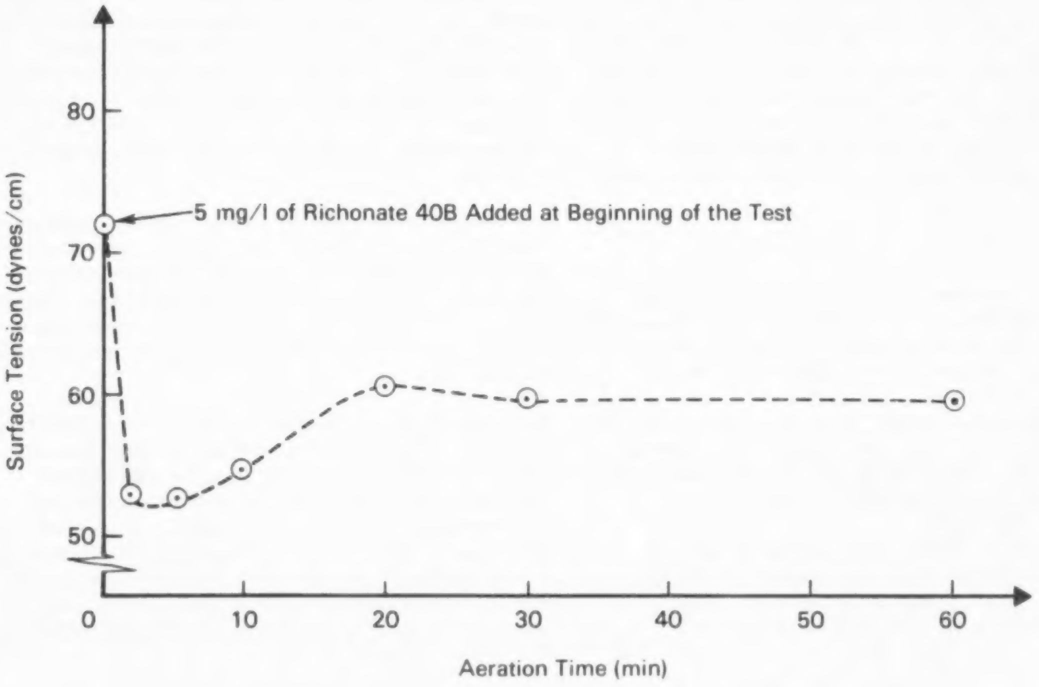


Figure 10. Considerations for Alpha and Beta Measurements

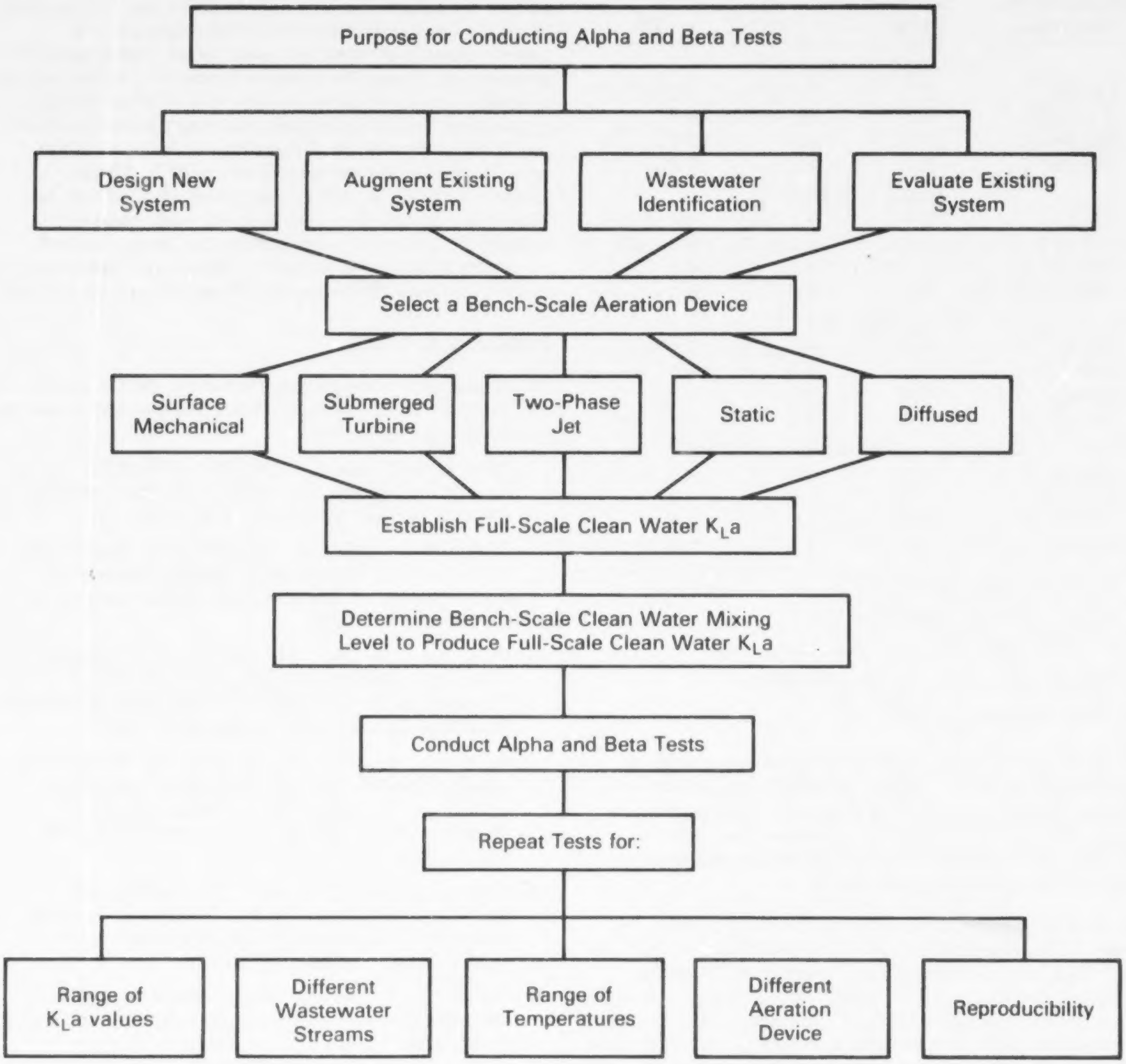

Purpose for Conducting Alpha and Beta Tests
Design New System
Augment Existing System
Wastewater Identification
Evaluate Existing System
Select a Bench-Scale Aeration Device
Surface Mechanical
Submerged Turbine
Two-Phase Jet
Static
Diffused
Establish Full-Scale Clean Water K_La
Determine Bench-Scale Clean Water Mixing Level to Produce Full-Scale Clean Water K_La
Conduct Alpha and Beta Tests
Repeat Tests for:
Range of K_La values
Different Wastewater Streams
Range of Temperatures
Different Aeration Device
Reproducibility

Table 5. Summary of Oxygen Saturation Measurements[1] (mg/l)

Measurement Technique	Distilled Water	Solution Strength 1%	 2%
		NaCl @ 20.5°C	
YSI 57[2]	9.1	9.0 (8.5)[3]	8.55 (7.5)[3]
WS 330[4]	9.1	9.1 (8.65)	8.7
Winkler 1	9.1	8.6	7.75
Winkler 2	9.1	8.7	7.85
		Na_2SO_4 @ 20.5°C	
YSI 57	9.1	9.4 (8.7)	9.4 (8.2)
WS 330	9.1	9.4 (8.7)	9.4 (8.2)
Winkler 1	9.1	8.7	8.3
Winkler 2	9.1	8.7	8.2
		$(NH_4)_2 SO_4$ @ 19.0°C	
YSI 57	9.4	9.5 (8.9)	9.1 (8.0)
WS 330	9.4	9.5 —[5]	9.1 —
Winkler 1	9.4	2.2	—
Winkler 2	9.4	1.2	—
		$C\ H_{12}O_6\ H_2O$ (Dextrose) @ 21.5°C	
YSI 57	8.9	8.8	8.7
WS 330	8.9	8.8	8.5
Winkler 1	8.9	8.0	8.0
Winkler 2	8.9	8.0	8.0

[1] Temperatures vary by test, but not within a test.

[2] Yellow Springs instrument (21).

[3] ( ) indicates corrected measurement.

[4] Weston and Stack instrument (20).

[5] No measurement taken.

Association (1) concluded that it is extremely difficult to accurately determine alpha, making the steady state test unacceptable as a standard method for rating aeration equipment or determining compliance of an aeration device with a performance guarantee. Similar conclusions were drawn by Kalinske (24).

In spite of the problems associated with the measurement of alpha and beta, several investigators (4) (16) (33) (40) have recommended sound procedures to follow, emphasizing those factors which affect measurement the most. Eckenfelder and Ford (17) describe in detail a test method for determining alpha and beta in the supernate of mixed liquor. Nogaj and Hurwitz (33) presented a method for determining alpha and beta in the supernatant without determining the oxygen uptake rate of the mixed liquor. Bass and Shell (4) describe a detailed step-by-step alpha measurement procedure with emphasis on five significant factors:

—Waste sample composition

—Type of aeration device used

—Test with suspended solids present

—Test at design temperature

—Test at design mixing level

Although Standard Methods (38) has a tentative procedure for determination of alpha and beta (207B5b and c), much work remains to be accomplished in the standardization of alpha and beta factor measurement procedures. Several key factors concerning measurement procedures are contained in Figure 10. Before detailed procedures can be developed, the major questions such as: 1) What defines equivalent mixing level? 2) What type of aeration device should be used? 3) Should surfactants be used in alpha testing and why? and 4) Are suspended solids in or out? must be addressed and answered. Only after a concensus has been reached regarding these major factors in alpha and beta measurements, can well defined standard procedures be adopted.

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Surface Aeration Equipment: Field Performance Testing Vs Shop Performance Testing

19

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The recent trend in surface aerator testing has been to conduct performance tests, i.e., oxygen transfer tests, in the manufacturer's test tanks. Several manufacturers have spent hundreds of thousands of dollars to develop shop testing facilities. Although they were constructed primarily to enable development and refinement of aeration equipment, the considerable cost involved forced equipment manufacturers to seek additional ways to utilize these facilities. It was only logical that the manufacturers would want to use shop facilities for the aerator performance testing often required by engineering specifications. It was reasoned that shop tests would reduce the cost of testing and consequently the owner's cost for the equipment, as well as provide controlled conditions for the performance tests. The idea caught on and many specifications allowed performance tests to be conducted in the shop. However, experience has proven that shop testing is not without its drawbacks. This paper will be devoted to a discussion of the problems associated with shop testing of surface aerators.

Problems Detected

Shop testing of surface aeration equipment to determine oxygen transfer capabilities has been allowed in recent years by the authors' firm, with the added requirement that tests of the physical aspects of the equipment be made after installation in the field. Typically, the specifications pertaining to the physical aspects of the aeration equipment have included:

1. *Adequacy of Baffling.* Baffling shall be provided as required to prevent interference between aerators and to satisfy specified power tests. If the aerators fail to satisfy performance requirements, additional baffling shall be installed or modifications made to the initial baffling and the tests repeated until satisfactory results are obtained.
2. *Power Testing.* Aerators shall be tested for power consumption throughout their operating range, with frequent checks for the influence of adjacent units and the adequacy of baffling. Power measurements shall be made with a recording wattmeter. With all units operating, the transient power consumption of each aerator shall not vary more than ±5% from the average value and shall be unaffected by the number of units operating in the basin.
3. *Mist and Spray Protection.* Under all operating conditions, liquid or spray shall not be thrown outside the basins nor shall mist or fine spray be created which could cause moisture or ice to collect on adjacent equipment, walkways or other structures.

Shop performance tests are generally assumed to be conducted under more controlled conditions than field tests. In an attempt to simulate field conditions, shop test tanks are supposed to be approximately equal in volume to the aeration basin volume influenced by one aerator. Yet, there are often considerable differences between the results of shop tests and field tests. This has been particularly true of power consumption, where the power consumption of aerators as installed in the field has often exceeded the limitations set forth in the specifications and occasionally exceeded the rating of the motors. The only recourse to correct power consumption in the field is to modify the aerator impeller or adjust the impeller submergence. If the oxygen transfer testing was conducted in the manufacturer's test tank, the aerating capability of the field modified equipment will be unknown unless additional oxygen tests are conducted in the field.

Field tests have also pointed out the need for added baffling in the aeration basins, in addition to that used in shop tests or in addition to that anticipated by the manufacturer. In one field test consistent and reliable oxygen transfer data could not be taken until the baffling on the basin was corrected. Mist and spray problems have also been obvious in the field during non-steady state testing where shop tests failed to indicate such a problem. The mist and spray developed by a group of aerators in an aeration basin cannot be duplicated by a single aerator in a test tank. Also, the mist and spray is much more obvious after an aeration basin is put into service than during aerator tests with clean water.

Case Histories

Six different installations in which the field test results differed significantly from the shop test results or from the manufacturer's expected results based on earlier shop tests are summarized in Tables 1 and 2. Equipment installed in these projects was supplied by five different manufacturers. Three different basic aerator impeller designs were used. Draft tubes were used in all of these installations, and all impellers were designed to be sensitive to variations in liquid level. All performance tests were conducted with the non-steady state method.

Installation A. Several problems were encountered with this installation, most notably; 1) a variation in aerator

Table 1. Characteristics of Aerator Installations

			Impeller Characteristics				
Installation Code	Number Aerators per Basin	Aerator Size, kW (hp)	Type	Diameter, m (ft)	No. of Blades	Maximum Rotational Speed, rpm	Volumetric Energy Input(1), kW/1000 m^3 (bhp/1000 ft^3)
A	3	130 (175)	Cone	2.84 (9.33)	8	37	82.1 (3.12)
B	4	45 (60)	Cone	1.78 (5.83)	12	56	51.3 (1.95)
C	4	75 (100)	Cone	2.29 (7.50)	8	47	67.4 (2.56)
D	9	75 (100)	Flat(2)	2.62 (8.58)	6	45	50.8 (1.93)
E	9	75 (100)	Flat	2.74 (9.00)	8	47	70.8 (2.69)
F	4	130 (175)	(3)	3.00 (9.83)	16	42	73.5 (2.79)

(1)Maximum motor output per unit aeration basin volume.

(2)Vertical flat blades, curved or backswept.

(3)Partially closed cone.

Table 2. Basin Dimensions and Volumes

	Shop Test Basin					Aeration Basin			
Installation Code	Length, m (ft)	Width, m (ft)	Dia., m (ft)	Depth, m (ft)	Volume, m^3 (ft^3)	Length, m (ft)	Width, m (ft)	Depth, m (ft)	Volume per Aerator, m^3 (ft^3)
A	10.9 (35.8)	10.7 (35.0)	—	9.1 (30.0)	908 (32,060)	32.0 (105.0)	10.9 (35.8)	9.1 (30.0)	908 (32,060)
B	13.7 (45.0)	13.7 (45.0)	—	5.0 (16.3)	933 (32,930)	25.3 (83.0)	25.3 (83.0)	4.7 (15.5)	756 (26,700)
C	13.7 (45.0)	13.7 (45.0)	—	4.9 (16.0)	918 (32,400)	29.0 (95.0)	23.5 (77.0)	4.9 (16.0)	829 (29,260)
D	18.3 (60.0)	15.2 (50.0)	—	6.4 (21.0)	1,784 (63,000)	39.6 (130.0)	39.6 (130.0)	6.1 (20.0)	1,063 (37,550)
E	—	—	21.3 (70.0)	4.3 (14.2)	1,548 (54,650)	41.1 (135.0)	41.1 (135.0)	5.2 (17.2)	986 (34,830)
F	—	—	—	—	—	32.0 (105.0)	31.7 (104.0)	5.8 (19.0)	1,469 (51,870)

power consumption relative to impeller submergence from shop test conditions to field conditions and 2) the installation of different equipment in the field from that tested at the shop.

In the shop test, one aerator was tested in a tank with exactly the same effective dimensions as the zone influenced by one aerator in the aeration basin. The proposed aeration equipment, including a 4.03 m (11.83 ft) draft tube, was installed in the tank for oxygen transfer testing. During pretests, however, the aerator design was found to cause excessive spray. Modification of the aerator to correct the problem resulted in a smaller diameter impeller. The modified impeller was tested at 46 and 34 rpm, with the original 4.03 m diameter draft tube in the basin. Final performance tests on this equipment configuration resulted in the data shown in Figure 1.

The shop test results indicated that the aerator transferred oxygen in excess of that required. The manufacturer suggested that the impeller speed be reduced to reduce power consumption through the two speed ranges. Estimates of the performance of the impeller at the reduced speeds of 37 and 27.8 rpm were made by the manufacturer based on the results of the shop test. These curves are also shown in Figure 1.

The aerators were installed in the field to operate at the reduced speed. The diameter of the draft tubes installed in the field was 2.84 m (9.33 ft), which is 0.76 m (2.5 ft) smaller than used in the shop test. The manufacturer had routinely reduced the draft tube diameter to coincide with the reduction in impeller diameter. The units were tested in the field for power consumption. The results of the first test are shown in Figure 2, along with the power curves estimated by the manufacturer. Closer inspection of the field-installed aerators revealed that the impellers as installed were different from the unit tested in the shop. Modifications were made to all the impellers in one aeration basin, and the units were retested. The results of this second field test are also shown in Figure 2. The resulting values were still approximately 18% higher than predicted by the shop tests. Further modifications to the impeller to reduce the power consumption in the field are still being considered.

It was expected that, since the shop test tank was an exact duplication of the zone of influence of one aerator in the field, the shop test results would directly relate to the field test results. This does not appear to be the case. However, the effect of a different draft tube diameter on the power consumption and the accuracy of the manufacturer's estimated power curve is unknown. It is apparent that with further modification the impeller can be made to approximate the power consumption measured during the shop tests. The effect of the different draft tube diameter and a possible impeller modification on the oxygen transfer characteristics of the aerator will remain unknown, however, without subsequent field performance testing.

Installation B. The principle problem encountered with this installation was power consumption. The power consumption in the field was approximately 35% more per aerator than measured in the shop tests at the same submergence. The power consumption curves from the two tests are indicated in Figure 3. In order to correct the problem, the manufacturer reduced the impeller blade depth from 152 mm (6 in.) to 127 mm (5 in.) and raised the aerator 51 mm (2 in.) out of the water. Subsequent testing indicated that the modifications affected power consumption as desired, but also caused a power variation in excess of 5% from the average. To correct this, the draft tubes were raised 178 mm (7 in.) to provide better baffling of the aerators.

Modification of the aerators and draft tubes resulted in a power draw at high speed very similar to that observed in the shop tests, as shown in Figure 3. However, the modifications lowered the low speed power consumption well below the shop test results. With the 229 mm (9 in.) liquid level variation specified for the basin, it is possible that the power consumption will be continuous throughout the range of liquid level variation and speed change. It is doubtful, however, whether the unit could be operated at much less than minus 127 mm (5 in.) submergence (127 mm emergence) at low speed.

How the modifications affected the oxygen transfer characteristics of the aerators is unknown. During the course of the modifications, the manufacturer supplied limited data to demonstrate the independence of oxygen transfer from the impeller blade depth. While this conclusion appeared possible, had field performance testing been specified, the oxygen transfer capabilities of the aerators would have been ascertained more definitely.

Installation C. An increase in power consumption from shop conditions to field conditions was also encountered in this installation. The impeller blades were trimmed in the field from 267 mm (10.5 in.) used in the shop tests to 203 mm (8 in.) in order to simulate shop power consumption. Power curves for the aerators from both shop and field tests are shown in Figure 4. Again, trimming the impeller had a greater effect on low speed power consumption than on the high speed power consumption but the power draw remained continuous over the entire range of liquid level and speed changes.

Oxygen transfer tests were performed in the shop tests and the field tests; the results of both are presented in Figure 5. At high speed, the oxygen transfer characteristics of the modified aerators installed in the field were similar to those of the original unit tested in the shop. At low speed, the field units exhibited a somewhat lower transfer capability than did the original unit in the shop. At both speeds, the aerators as installed in the field appear to be slightly more efficient than the original unit

Figure 1. Power Consumption by Aerator A — Shop

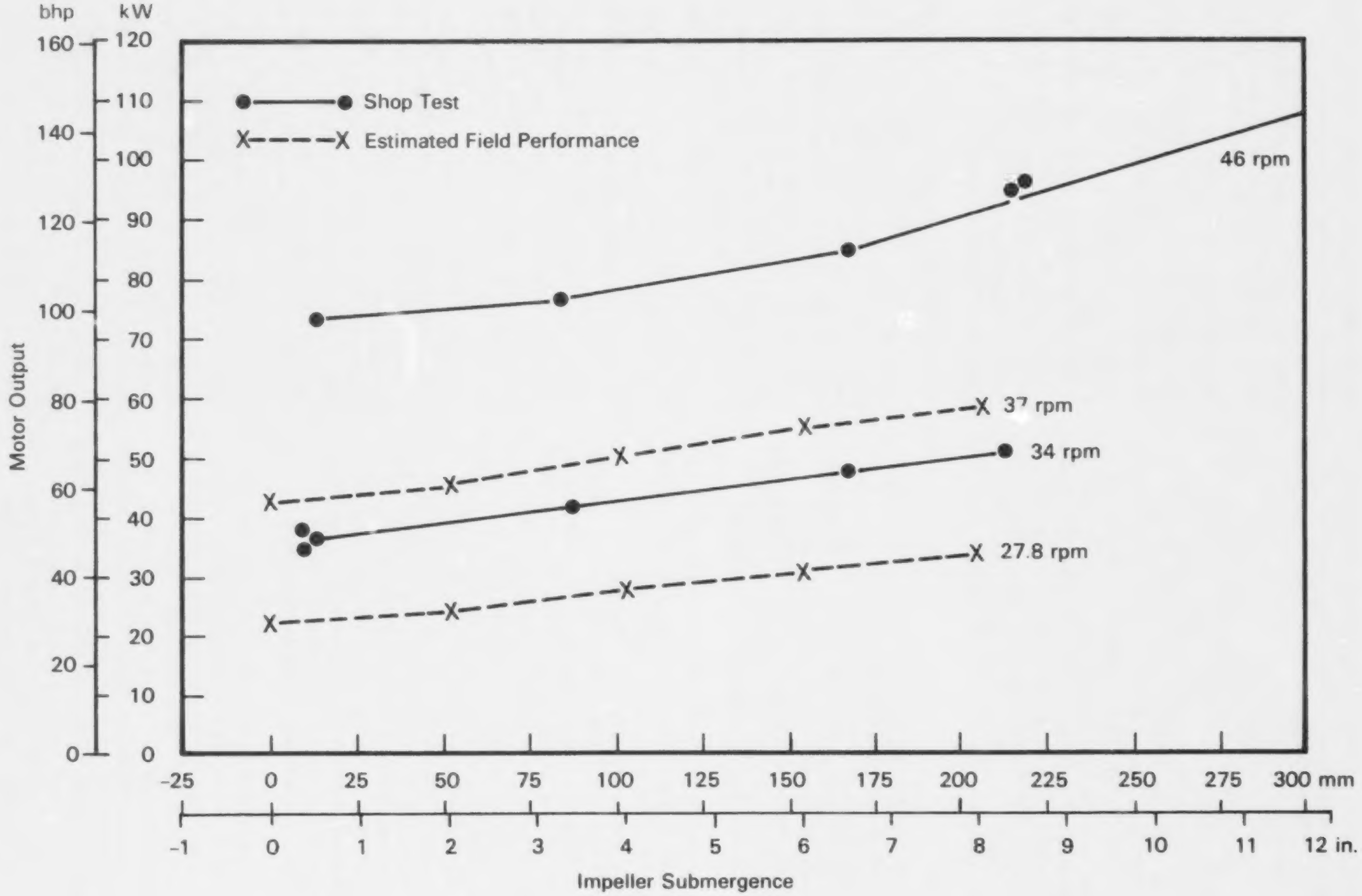


Figure 2. Power Consumption by Aerator A — Field

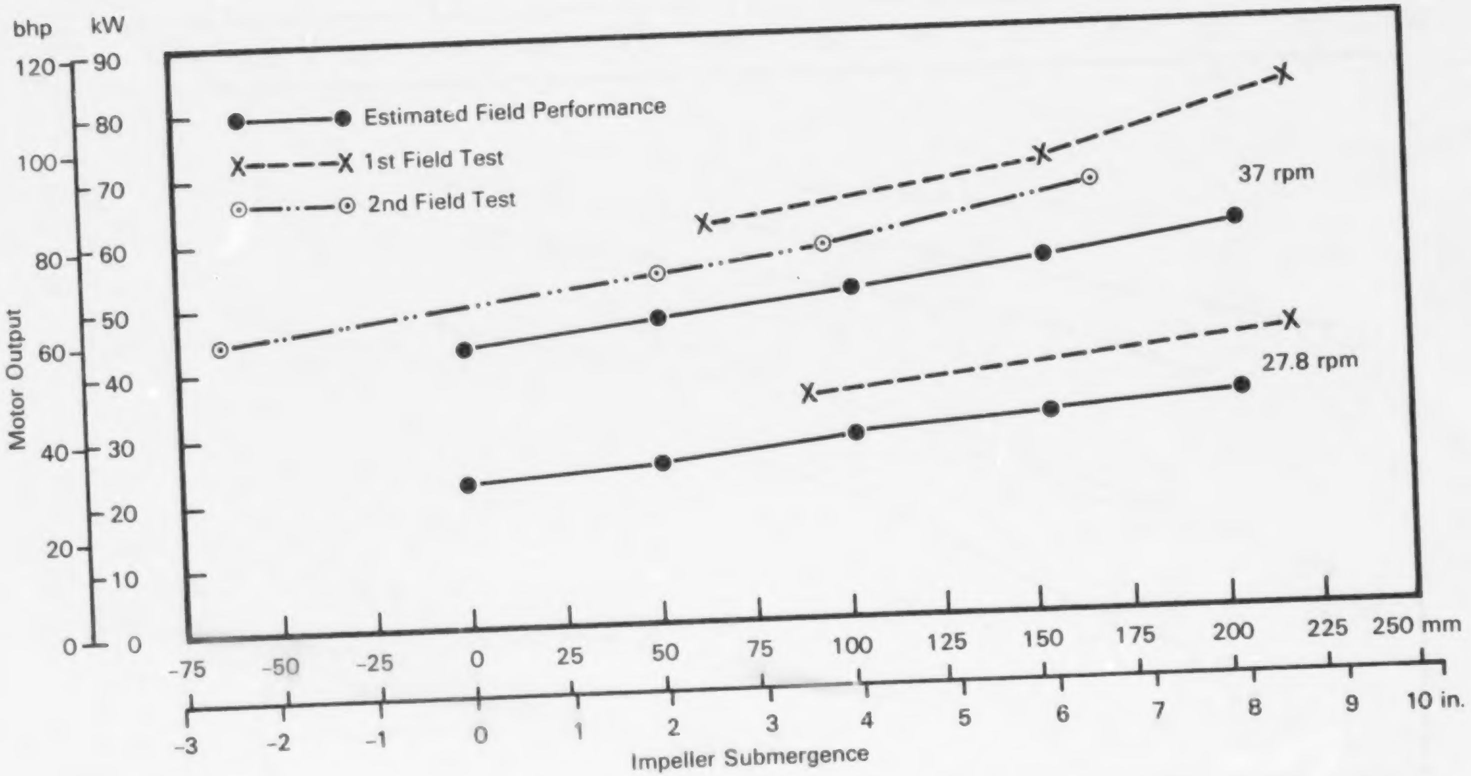


Figure 3. Power Consumption by Aerator B

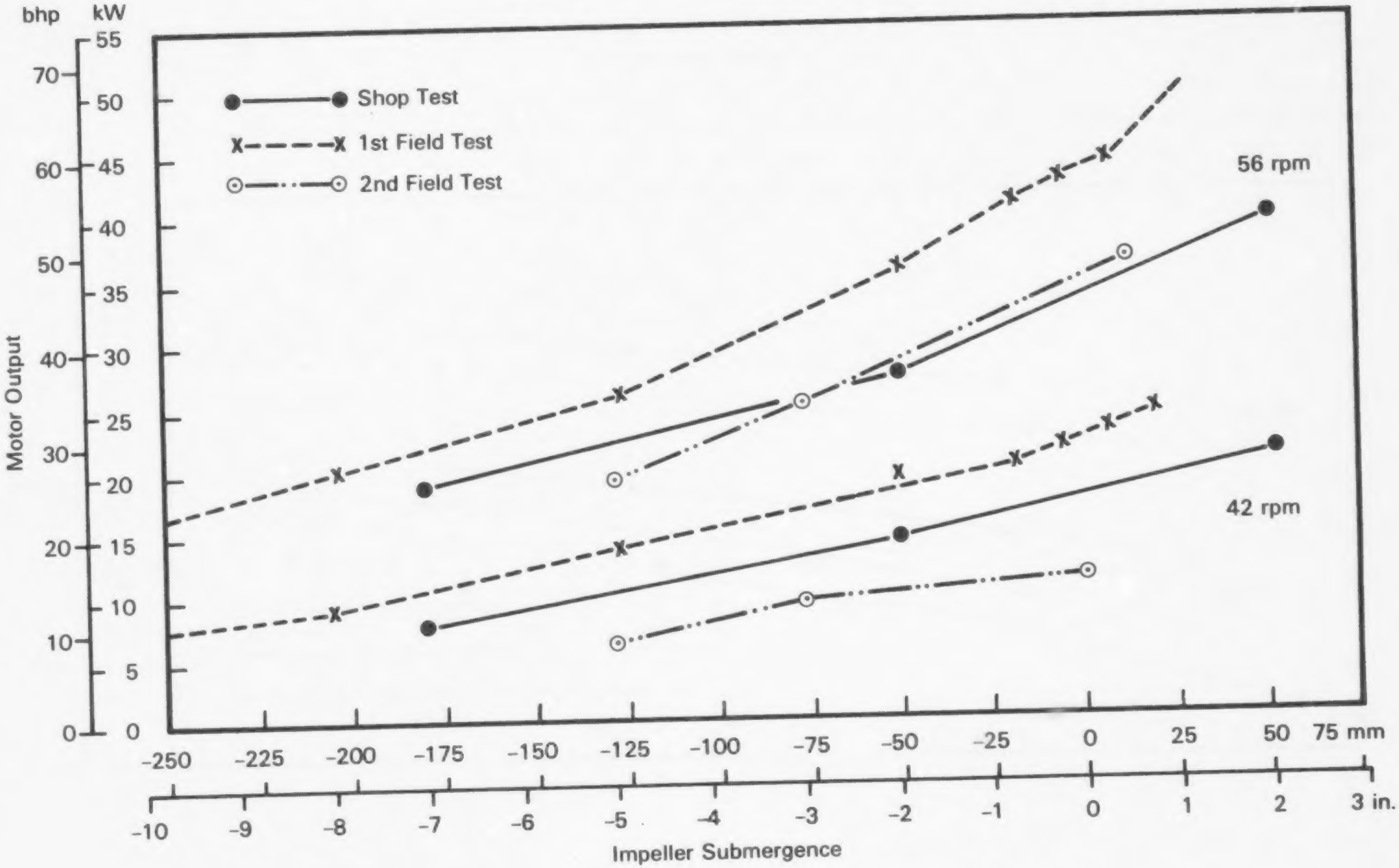


Figure 4. Power Consumption by Aerator C

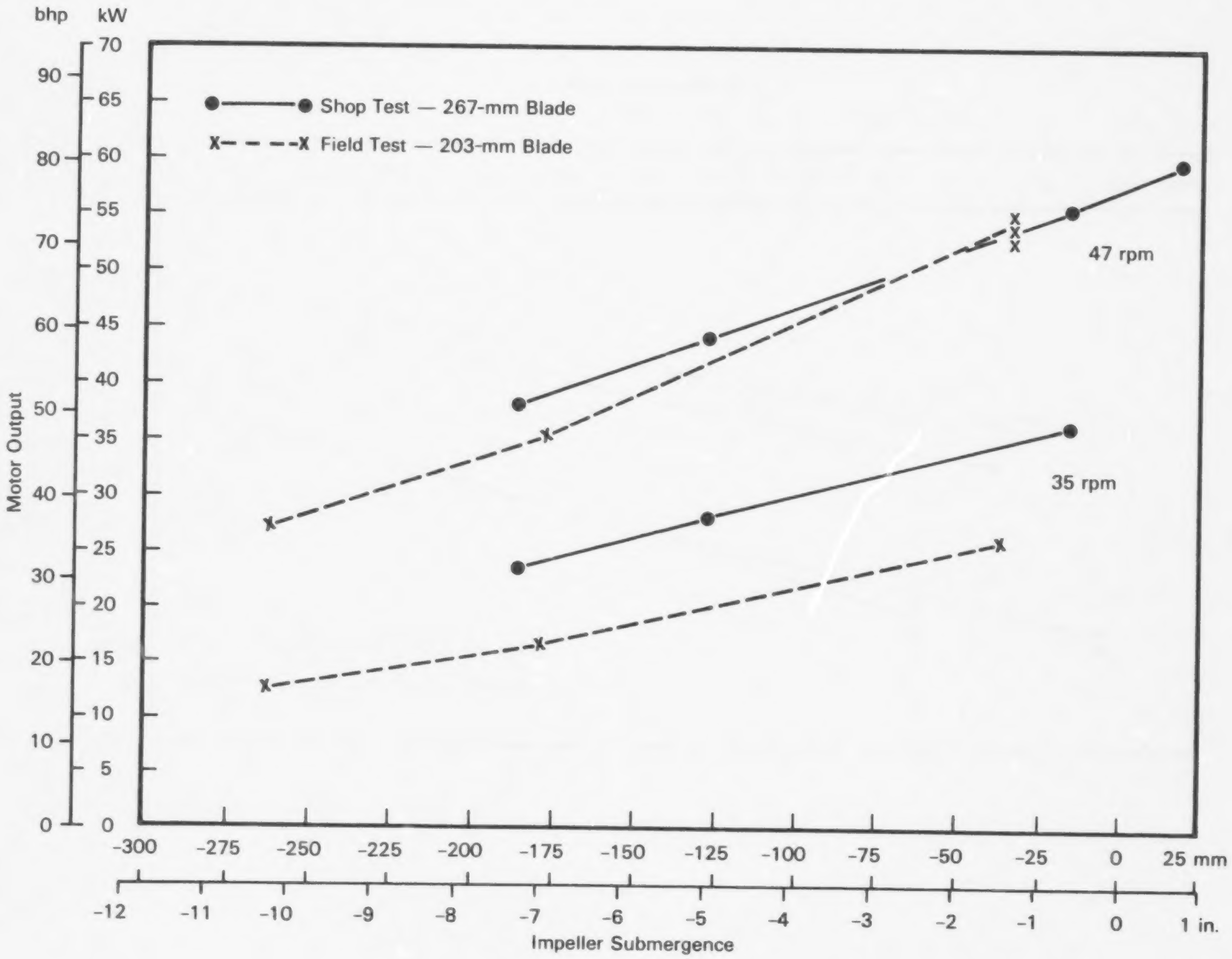
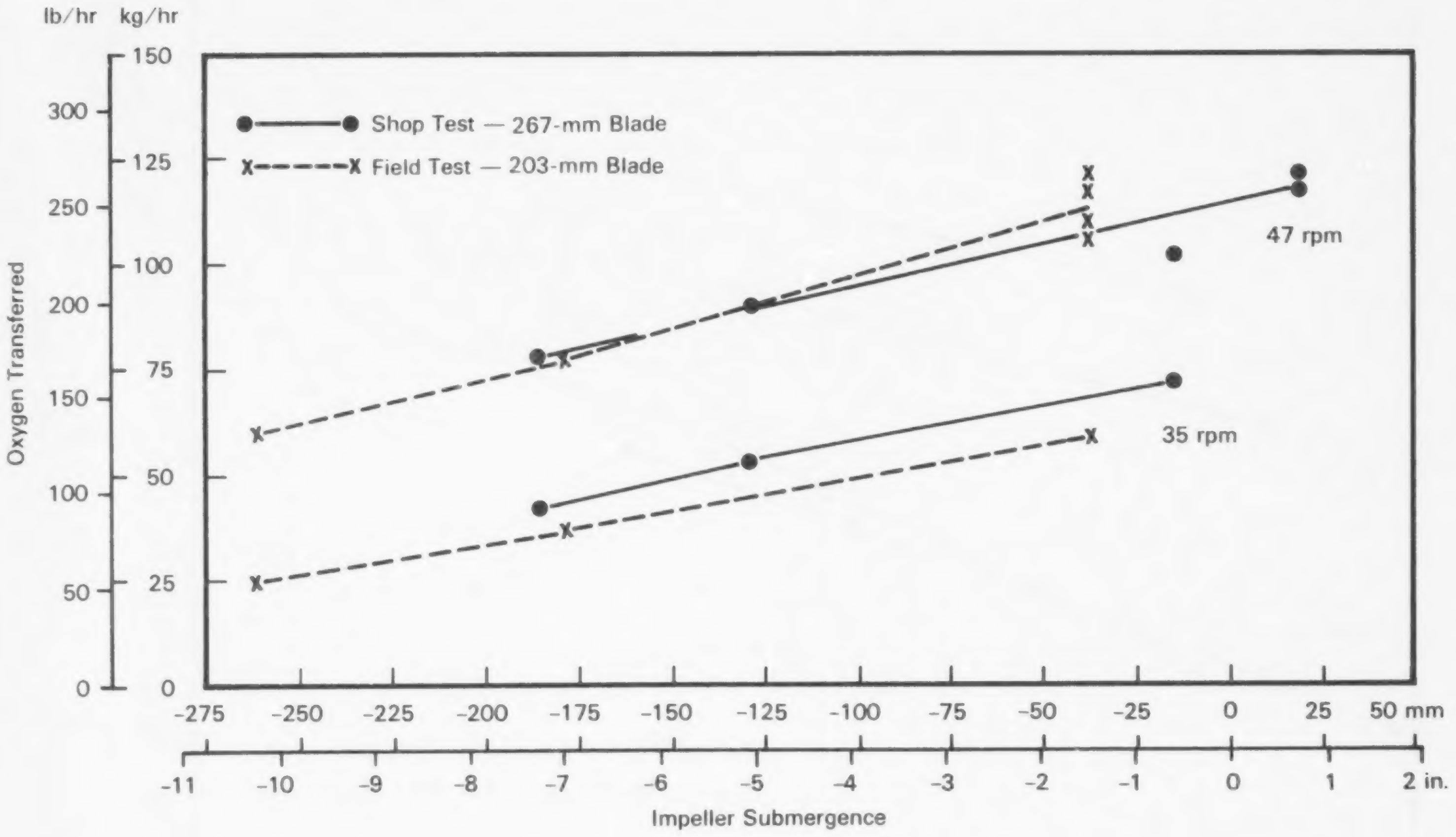


Figure 5. Oxygen Transfer Characteristics of Aerator C



in the shop tests. However, the difference may be the result of the temperature correction factor. The shop test was conducted at approximately 27°C liquid temperature, whereas, the field test was conducted at approximately 17°C liquid temperature.

Installation D. Here again, field power consumption was found to be greater than the shop power consumption at equal impeller submergence. As a result of this problem two field power tests were conducted, the second following modification of the aerator installation. The differences between the shop and initial field power test values were greater than in any other installation known to the authors. As much as 50% more power was consumed by the aerators in the field (when all nine units were operating) than was observed in the shop test at the same impeller submergence. These data are presented in Figure 6, as are the results from operating only the aerator in the center of the aeration basin. Even greater power consumption was observed in this mode of operation.

In order to reduce power consumption to the specified range, the submergence of the impellers was reduced by raising the aerators approximately 150 mm (5 in.). The draft tubes were raised a similar amount to maintain the same relationship between the impeller and draft tube as used in the shop test. No other modifications were made to the aerators. Power tests were again conducted, as were oxygen transfer tests. The results of the power tests are shown in Figure 7. The power consumption was reduced as desired, and the slope of the curve remained relatively unchanged by the modification of the impeller submergence. Although the manufacturer assured the owner and engineer that the oxygen transfer efficiency would be unaffected by the change in impeller submergence, a 10 to 15% decrease in transfer efficiency from shop test results was observed, as indicated in Figure 8. Had the specifications required a transfer efficiency of 0.5 kg oxygen/MJ (3.0 lb oxygen/bhp·hr), an efficiency achievable by most aerators currently manufactured, it is doubtful whether the equipment would have passed the oxygen transfer test following modification.

Installation E. One final example of change in power draw characteristics from shop to field conditions is provided by installation E. While the previously discussed installations all exhibited an increase in power consumption when the aerators were installed in the field, the aerators at installation E drew less power per aerator at a given submergence in the field than in the shop test tank. To rectify this problem, the manufacturer lowered the impellers 89 mm (3-1/2 in.) into the water by placing spacers in the drive shaft couplings and adjusting the jacking studs on the gear boxes. As shown in Figure 9, the power consumption characteristics of the installed aerators as determined by two field tests were the same as those of the unit tested in the shop, except for the difference in impeller submergence.

The results of oxygen transfer tests conducted on the aerator are shown in Figure 10. The transfer efficiency of the aerators as installed in the field was slightly greater than the shop test efficiency, particularly at low liquid levels. However, a substantial difference in test water temperature may have also affected these results. The shop tests were conducted at a water temperature of 5-1/2°C, while the field tests were conducted at 25 to 27°C. In general, the oxygen transfer characteristics of the aerators were virtually the same in the field and in the shop.

Installation F. Specific shop tests were not conducted for this installation. Field results were predicated on earlier shop tests. Field testing indicated that the aerators probably could transfer the required quantity of oxygen into the water. However, the basin baffling provided by the draft tubes was not sufficient to prevent surging of the aerators, which in turn affected the oxygen transfer characteristics of the units. The surging was probably the result of interaction of the aerators, a condition not simulated in shop tests.

Drifting of aerator power consumption prior to field modifications is shown in Figure 11. The variation in power consumption from the average was approximately ±11.4% at high speed. Under these conditions, the aerators were unable to transfer the specified amount of oxygen. Baffling of the basin resulted in a reduction of the power variation of the aerators to ±3.3% at high speed. The baffling was not as effective on low speed power variations, although the variance was held to approximately ±10%. With the installation of the baffles, the aerators satisfied the specified oxygen transfer requirements, apparently as a result of more efficient use of power. If performance testing had been by shop tests, it is doubtful whether the oxygen transfer capabilities of the units in the field would have been known for certain. In this case, performance testing was not conducted until all modifications had been made so that the results truly represented the capabilities of the installed aerators.

Discussion

From the six cases presented, it is evident that the performance of a surface aerator installed in an aeration basin with as many as eight other aerators is not the same as when it is operated in the isolated conditions of a shop test tank. In most of the examples cited, the power draw of the aerators at a given impeller submergence increased when the units were installed in the field. Installation E demonstrated that the change in power consumption can just as easily be downward.

The reason why field power consumption is often different from shop power consumption is not readily apparent. It does not appear to be related to impeller design or manufacturer. The largest change in power consumption was exhibited by the aerators of installation D, which also had the largest difference between test

Figure 6. Power Consumption by Aerator D — Shop

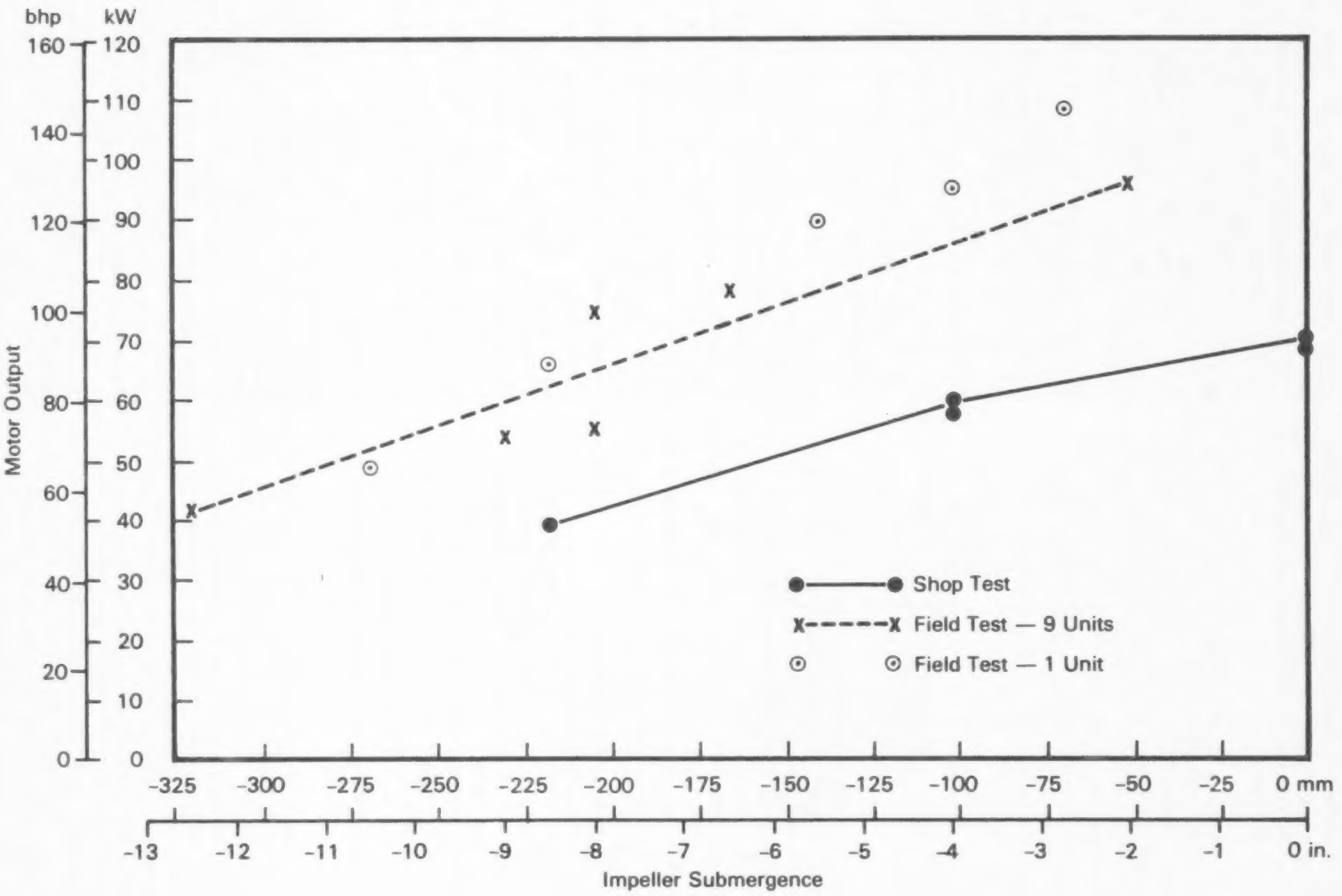


Figure 7. Power Consumption by Aerator D — Field

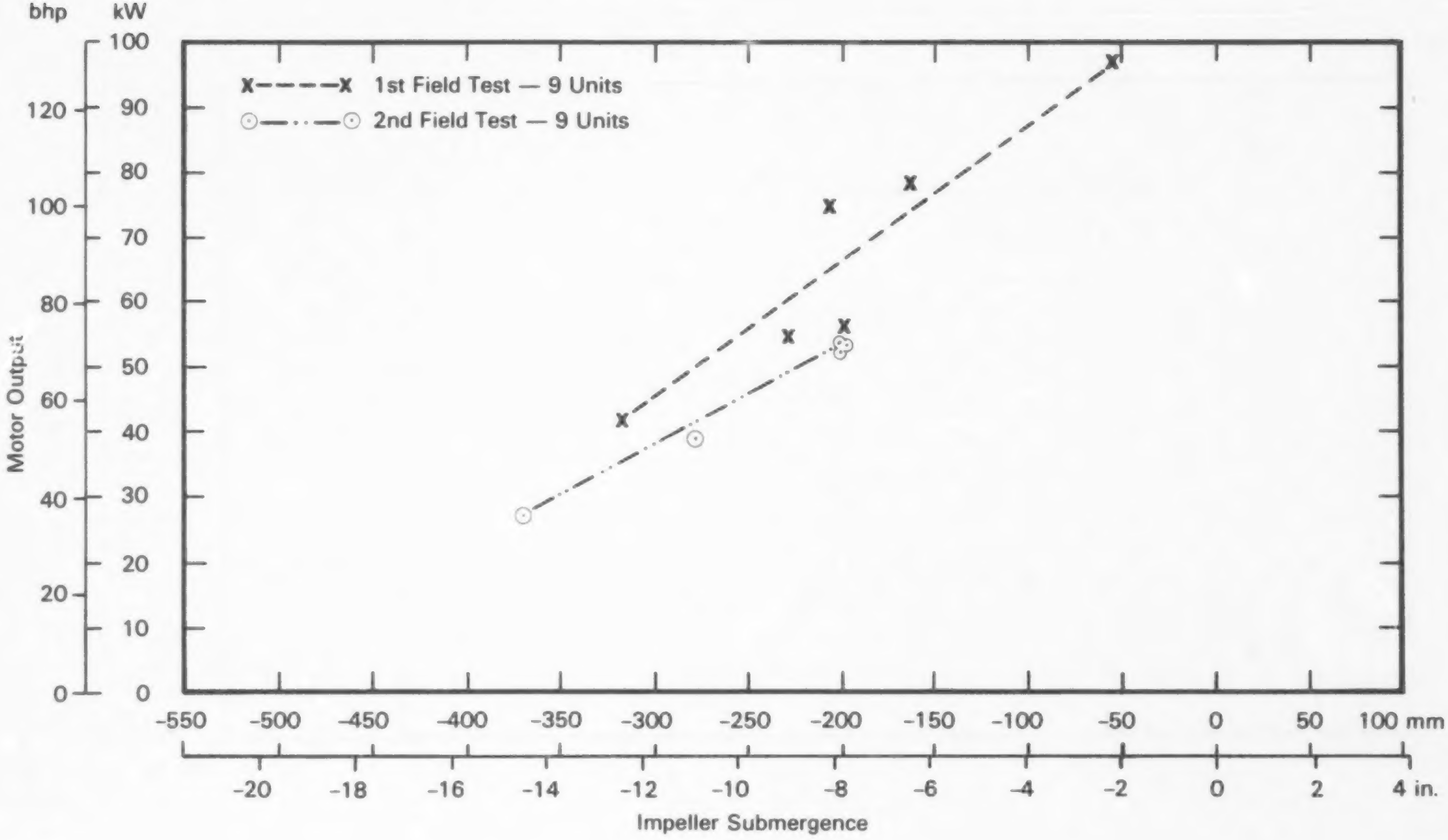
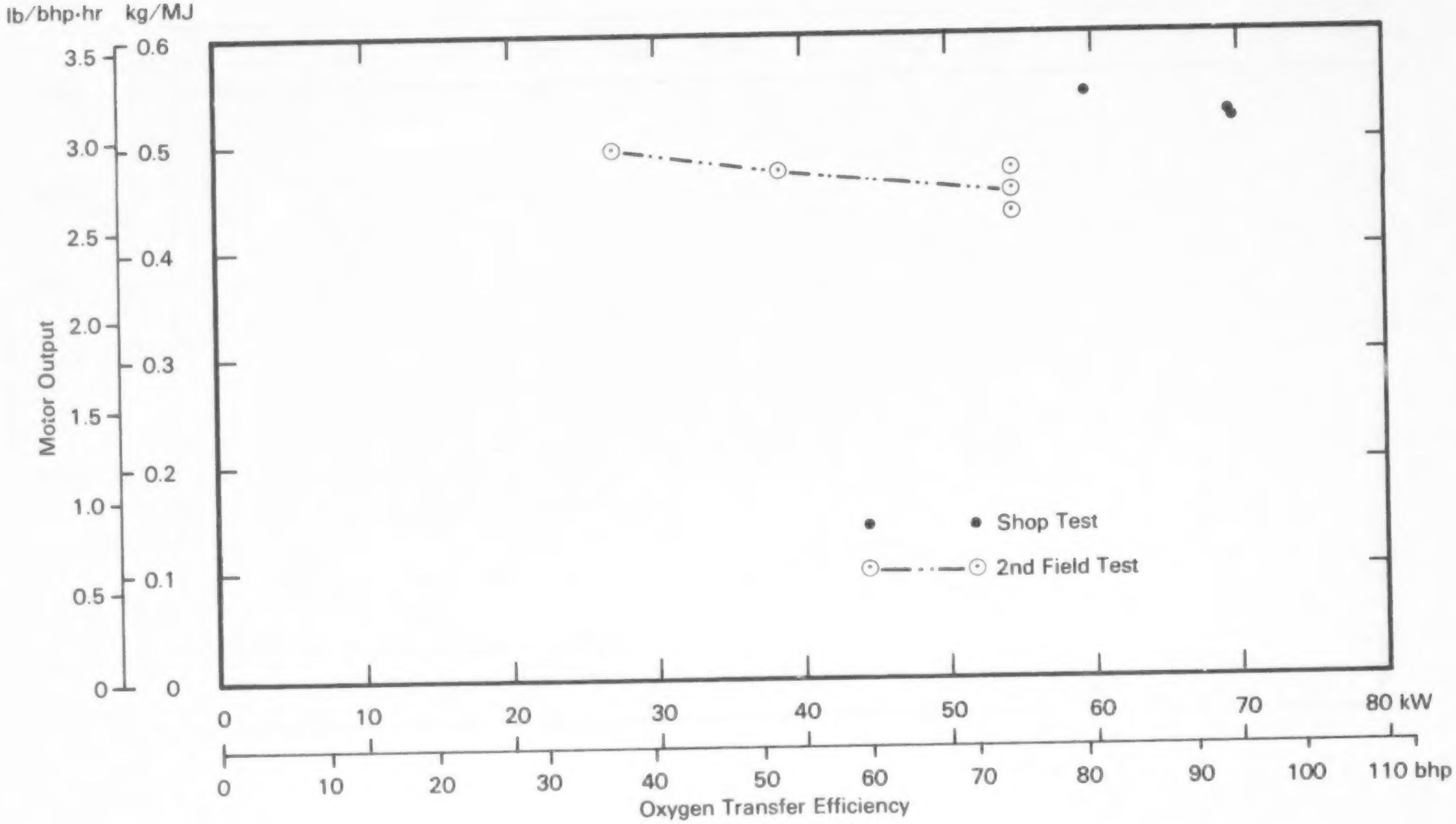


Figure 8. Oxygen Transfer Efficiency of Aerator D



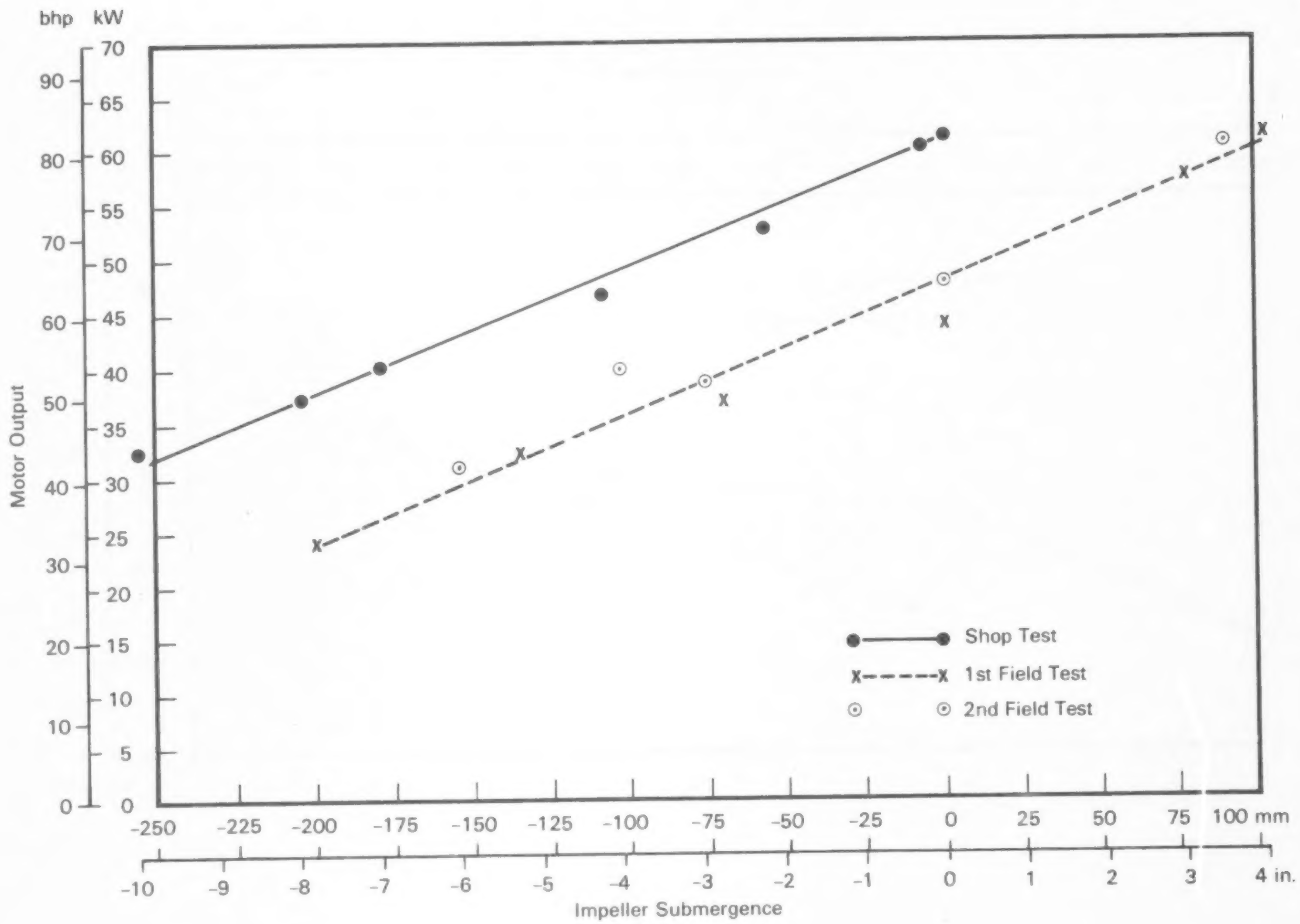


Figure 9. Power Consumption by Aerator E

Figure 10. Oxygen Transfer Characteristics of Aerator E

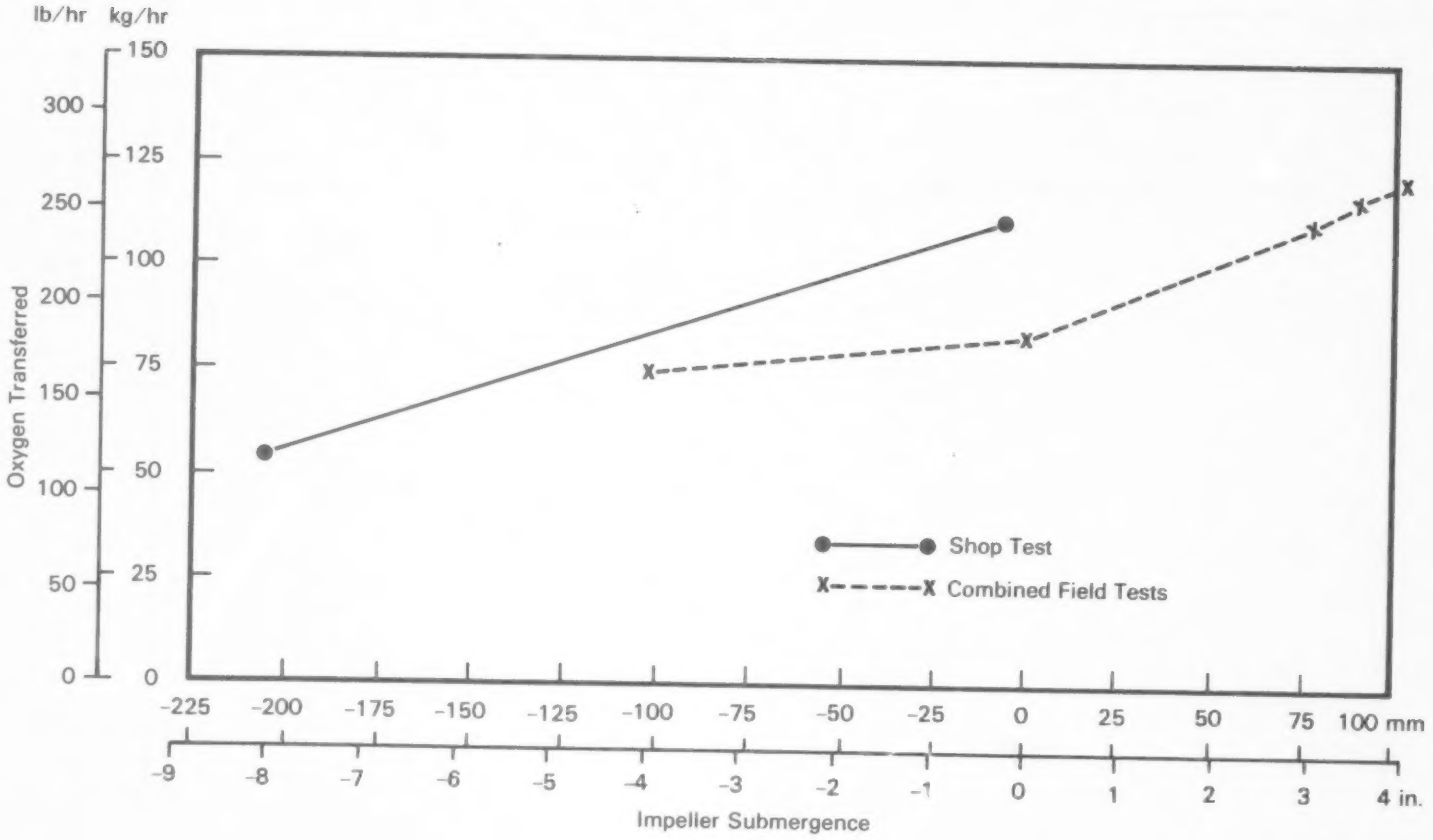
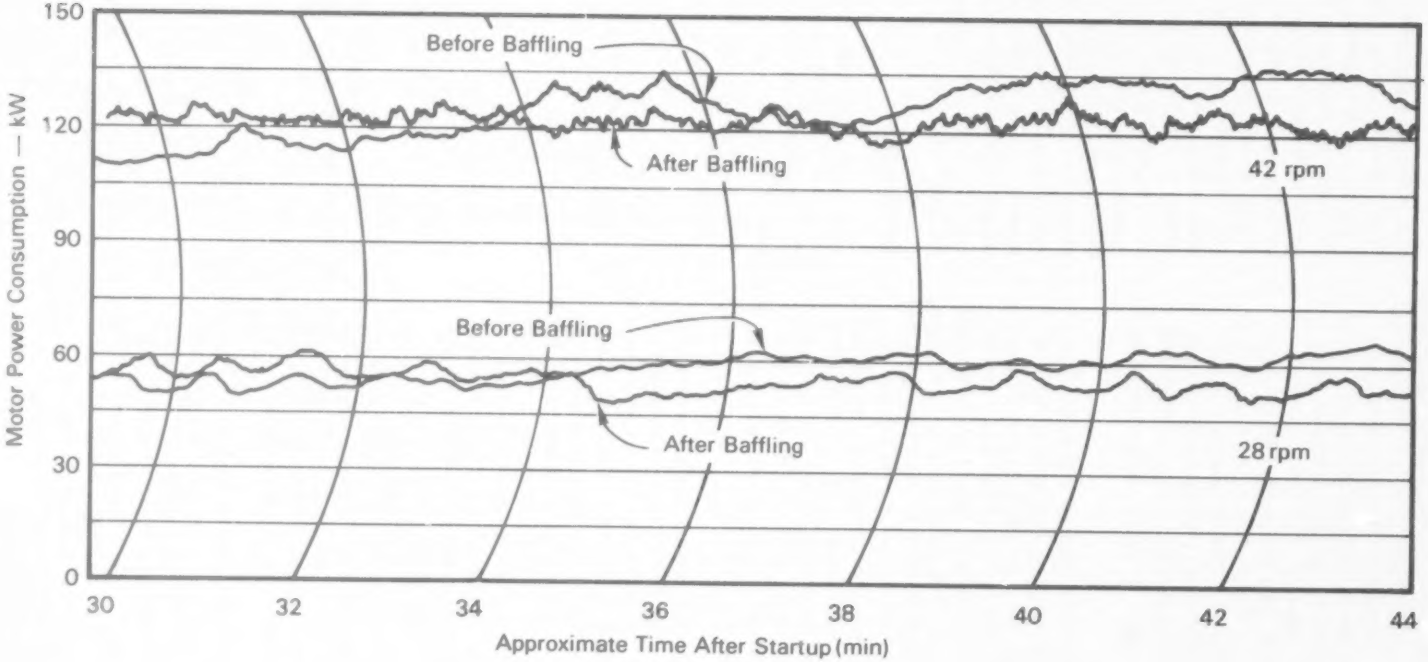


Figure 11. Power Consumption by Aerator F



tank volume and zone of influence volume in the aeration basin. The test tank volume was 68% larger than the zone of influence of one aerator. The power consumption increased from shop to field conditions by 49%. Yet, there does not appear to be a direct relationship between volume change and power consumption change. There was no change in the volume aerated between shop tests and field tests of aerator A, but the power consumption in the field was approximately 18% greater than that recorded in the shop tests. A 9% reduction in aeration volume resulted in a 33% increase in power consumption between shop and field tests for aerator C, while a 36% volume reduction from shop to field tests resulted in a 22% reduction in power consumption for aerator E.

Another possibility is that the change in power consumption from shop to field conditions is a function of the volumetric energy level of the aeration basins. The cases reported all had a relatively high energy level, from 50.8 to 82.1 kW/1000 m^3 (1.93 to 3.12 bhp/1000 ft^3) based on motor output. However, five other installations having volumetric energy levels from 50.8 to 70.3 kW/1000 m^3 (1.93 to 2.67 bhp/1000 ft^3) apparently performed as expected, since no field modifications were required prior to performance testing. Furthermore, at the time of this writing, there were two other installation with volumetric power levels of approximately 39.5 kW/1000 m^3 (1.5 bhp/1000 ft^3) known to be consuming more power in the field than the shop tests indicated.

In addition to often failing to predict the performance of an aerator in the field, shop tests can also be misleading. Equipment installed in the field may not always resemble the equipment or configurations used in the shop tests. As was the case for most of the installations discussed, the zone of influence for an aerator installed in the field is seldom the same as for the configuration of the shop test tank. This is usually understood by the engineer before shop tests are conducted. A more subtle change between shop and field conditions is modification of equipment before field installation. While this is not frequent, it has occurred. In the case of installation A, it is not known how modification of the equipment may have affected the performance of the equipment, or whether it did affect it. It is known that there was difference in power consumption between the shop test and field thest and that further tests are necessary.

Two aspects of aerator testing that cannot be determined until the aerators are installed in the field are the interaction of the aerators and the mist and spray characteristics of the aerators. In the case of installation F, it is doubtful that the manufacturer expected power surging problems. In most instances, the basin baffling provided by draft tubes has been adequate to prevent surging. However, the field performance tests on aerator F demonstrated the need for additional baffling. Because performance tests were conducted in the field, the oxygen transfer characteristics of the aerators as installed in the baffled aeration basin are known.

It is difficult to assess the mist and spray characteristics of an aerator during shop tests, primarily because most problems with misting result from the interaction of all the aerators in a basin. Excessive mist and spray can usually be detected during field tests either by moisture on walkways and handrails, or if weather conditions promote rapid evaporation of moisture, by the deposition of chemicals from the test water on walkways, handrails and aeration equipment. If necessary, conclusive albeit messy proof of excessive mist and spray can be obtained by operation of the aerators with mixed liquor in the aeration basins. Regardless of the test for spray and mist, if the performance test is conducted in the field, it is possible to modify the aeration system to correct spray and mist problems before the aeration tests are conducted. In this manner, modifications that may affect the oxygen transfer characteristics of the aerators, such as lowering the mist shroud, will be included in results of the performance test of the aerators.

Conclusions

The experiences with shop testing of surface aerators where a single unit is tested, and field testing where all the aerators in an aeration basin are tested have led to the following conclusions:

1. Shop testing does not adequately predict the performance of aerators after they are installed in the field.
2. At a given impeller submergence, the power consumption of surface aerators installed in the field may vary as much as 50% from the power consumption measured during shop tests.
3. The change in aerator power consumption characteristics from shop tank to aeration basin is not predictable, nor does it appear to be directly related to the impeller design, the relationship of shop tank volume to zone of influence volume in the aeration basin, or the volumetric energy level in the aeration basin.
4. Both impeller modification and alteration of impeller submergence have been successfully used to correct field power consumption of aerators to shop test results.
5. There is evidence from two installations discussed that oxygen transfer may be proportional to the power consumption of the impeller, and slight modifications of the impeller do not alter the oxygen transfer efficiency of the impeller. A third installation, however, exhibited a 15% decrease in oxygen transfer efficiency as a result of modification of the aerator to correct power consumption.
6. Equipment installed in the field is not always identical to equipment tested in the shop.

7. Surging of the power consumption of aerators and mist and spray characteristics of aerators can only be determined in the field.
8. Field performance testing permits modification of impeller design or submergence, additions to basin baffling and corrections to conditions resulting in excessive mist and spray before performance testing. Unlike shop tests, field performance tests represent the aeration equipment as installed in the aeration basin.

Section IV. Evaluation of Respiring Systems

Oxygen Transfer in Closed Systems

20

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Introduction

In aeration, two basic equations are used to describe the oxygen transfer capabilities of a system. Equation 1, Henry's Law, relates the equilibrium (saturation) concentration of oxygen in solution, C^*, to the partial pressure of oxygen in the gas phase, p_{O_2}, by:

(1) $$C^* = Hp_{O_2}$$

where H is Henry's constant. Equation 2 relates the rate of change of oxygen concentration in the liquid to the driving force, the oxygen deficit, (C^*-C):

(2) $$dC/dt = K_La\,(C^*-C)$$

where C is the actual dissolved oxygen concentration, and K_La, the oxygen transfer coefficient, is determined by the type, size, and quantity of aeration devices.

When evaluating the transfer capabilities of open tank surface aeration systems, the partial pressure of oxygen in the atmosphere above the tank is constant. Thus, the saturation value, C^*, is constant and the analysis is relatively straight forward. In diffused or turbine aeration systems, the partial pressure of the oxygen changes as bubbles rise from bottom to surface due both to static pressure and O_2 transfer; thus, the saturation value differs from top to bottom of the tank. However, the influent gas partial pressure is constant since atmospheric air is utilized. Also, the buildup of other dissolved gases in the aeration tank is relatively small since when above saturation, they are stripped out of solution and released to the atomosphere.

In evaluating the oxygen transfer capabilities of closed aeration systems, whether surface or submerged aeration equipment is used, buildup of other gases occurs in both the liquid and gas phases, causing a reduction of the oxygen mole fraction. The resulting oxygen partial pressure in the gas phase cannot be predicted without conducting mass balances on both gas and liquid phases where consumption and production of all gases are properly accounted for.

Model Development

Development and verification of this approach was conducted initially for closed tank, staged, high purity oxygen systems (1) where the above effects had significant design implications. However, it is also applicable to closed tank air aeration systems since the same phenomena occur, only with a different influent gas composition.

Three gas transfer mechanisms occur when pure oxygen is introduced into a covered aeration tank: 1) oxygen is transferred from the gas to the liquid phase, 2) nitrogen originally present in the liquid phase is transferred to the gas phase, and 3) carbon dioxide produced by the biological reaction is transferred to the gas phase. In a sealed aeration tank where gas is vented only from the last stage, these latter two mechanisms cause the oxygen gas phase partial pressure to decrease from stage to stage.

Figure 1 presents a schematic of the closed system analyzed in this paper, a staged system with a gas phase flowing concurrently with the liquid phase. Both gas and liquid phases are completely mixed for each stage with gas vented only from the final stage. Two mass balance equations are constructed for each parameter in the system, one for the liquid phase and one for the gas phase as follows:

Liquid Phase

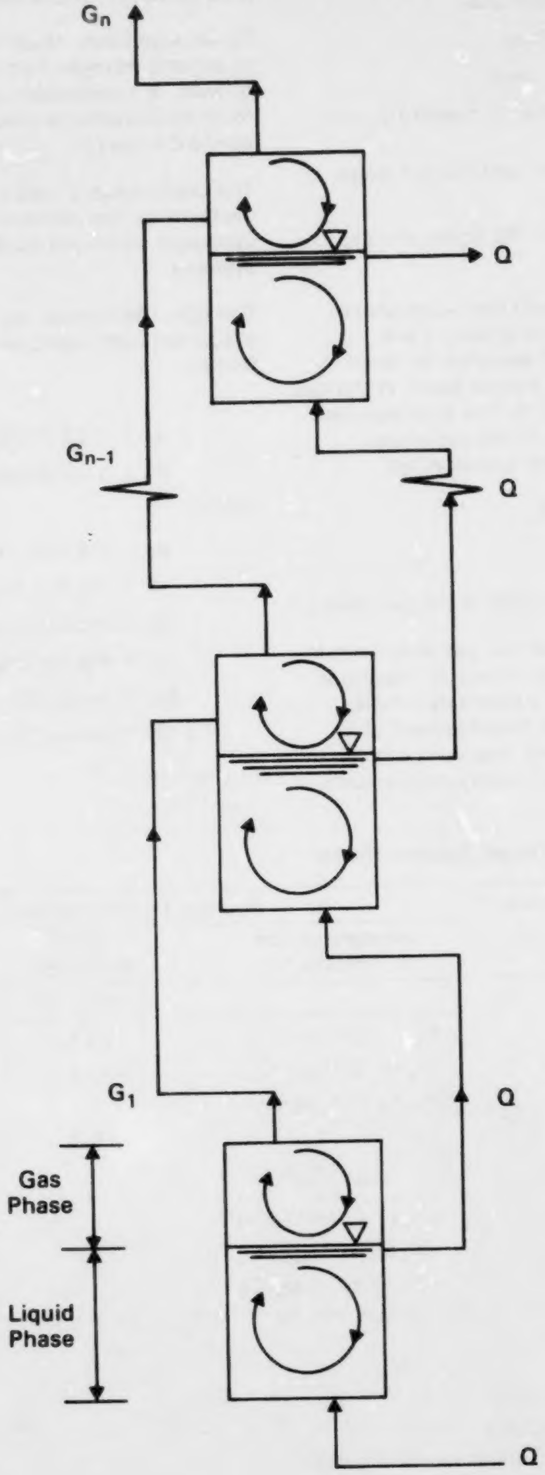
(3) $$dC_{i,n}/dt = (Q/V^l_n)\,(C_{i,n-1}-C_{i,n}) + \sum R^l_{i,n}$$

Gas Phase

(4)

$$dp_{i,n}/dt = (G_{n-1}p_{i,n-1}-G_n p_{i,n})/V^g_n + (V^l_n/V^g_n)\,(\bar{R}T/M_i)\sum R^g_{i,n}$$

Figure 1. Completely Mixed Series Reactor Schematic for Concurrent Flow of Liquid and Gas Phases



G_n
Q
"n"
G_{n-1}
Q
"2"
G_1
Q
Gas Phase
Liquid Phase
"1"
Q

where:

C, p = parameter concentration in the liquid and partial pressure in the gas

V^l, V^g = liquid and gas volumes

Q, G = liquid and gas flow rates

$\bar{R}, T, M$ = gas constant, absolute temperature, and molecular weight

i, n = species identification and reactor stage location

R^l, R^g = sources and sinks in the liquid and gas phases

For each parameter of concern, both the liquid phase concentration and gas phase partial pressure are unknown, with Equations 3 and 4 available for each. However, an additional unknown always exists in the gas phase equation, the gas flow rate, G. The final equation, defining a constant total pressure in the gas phase, allows simultaneous solution of the equation set:

$$\sum p_{i,n} = p_t - p_v \tag{5}$$

where:

p_t, p_v = total and vapor pressures of the gas phase

Because of the non linear nature of the gas phase equation, the equation set is normally numerically integrated to steady state, which has proven a relatively simple, convenient, and reliable technique. Development of a steady state solution which requires less computer time has been accomplished for the high purity oxygen aeration system.

Table 1 presents the source and sink terms used in Equations 3 and 4, incorporating interphase gas transfer, BOD oxidation, and endogenous respiration.

When significant alkalinity changes occur in solution due to organic nitrogen hydrolysis, ammonia utilization for growth, or nitrification, additional chemistry interactions must be included to properly evaluate the dissolved CO_2 concentration (2).

The coefficients a′ and b′, as well as the BOD removal coefficients, are obtained for typical bench- or pilot-scale biological treatment studies similar to air aeration systems

The CO_2 coefficients, a_2 and b_2, are related to the oxygen utilization coefficients by the respiratory quotient, RQ, as follows:

$$a_2 = 1.38\,(RQ)(a')$$

$$b_2 = 1.38\,(RQ)(b')$$

where:

a_2 = mg CO_2/mg BOD

a' = mg O_2 mg BOD

b_2 = mg CO_2/mg MLVSS·day

b' = mg O_2/mg MLVSS·day

RQ = moles CO_2/mole O_2

1.38 = (44 mg CO_2/mole CO_2)/(32 mg O_2/mole O_2)

Table 1. Sources and Sinks for Closed System Model

Species	Designation	Sources (+) and Sinks (-), (mg/l·hr)		
		Interphase Gas Transfer*	BOD Oxidation	Endogenous Respiration
			Liquid, ΣR^l	
O_2	C_1	$K_La\ (\beta H_1 p_1 - C_1)$	$-a'kX_vS$	$-b'X_v$
CO_2	C_2	$(K_La)_2\ (\beta H_2 p_2 - C_2)$	$+a_2kX_vS$	$+b_2X_v$
N_2	C_3	$(K_La)_3\ (\beta H_3 p_3 - C_3)$		
BOD	S		$-kX_vS$	
		Gas, $\sum R^g$		
O_2	p_1	$-K_La\ (\beta H_1 p_1 - C_1)$		
CO_2	p_2	$-(K_La)_2\ (\beta H_2 p_2 - C_2)$		
N_2	p_3	$-(K_La)_3\ (\beta H_3 p_3 - C_3)$		

*where: $(K_La)_2 = (D_{L2}/D_{L1})^{(1/2)} K_La$

$(K_La)_3 = (D_{L3}/D_{L1})^{(1/2)} K_La$

D_L = liquid phase diffusivity

k = first order reaction rate

X_v = volatile suspended solids concentration

Calibration

Prior to model application, calibration of the kinetic parameters in the model must be obtained. Calibration of the high purity oxygen model has been obtained, for both domestic and industrial wastewaters (1).

Figures 2 and 3, the model verification for the Batavia Phase III data, show the typical decrease in gas flow and oxygen partial pressure in successive stages. The behavior of the nitrogen concentration is interesting. In the first stage of the aeration tank, nitrogen is stripped out of solution into the gas phase, causing a drop in the liquid phase nitrogen concentration. However, in the second and third stages, the equilibrium shifts and nitrogen is transferred back to the liquid phase, causing a rise in the dissolved nitrogen concentration. Because of its high solubility, the majority of the CO_2 remains dissolved in solution, yielding effluent concentrations greater than 250 mg/l and a resulting effluent pH of 6.3. In the parallel air aeration system at Batavia, the pH in the aeration tank remained at approximately 7.1, the same as the raw wastewater, due to the continual stripping of CO_2 to the atmosphere.

The above model has also been applied to analysis of a four-stage pure oxygen pilot plant treating an industrial waste. Table 2 indicates that increasing F/M ratios required decreasing respiratory quotients, RQ, to effectively model the domestic and industrial waste data. This is due to either changing waste characteristics, or to incomplete oxidation at the higher loading rates.

Table 2. Effect of BOD Loading Rate on Respiratory Quotient

Waste	F/M (lb BOD/ lb MLVSS·day)	RQ (molesCO_2/moleO_2)
Domestic	0.55	1.00
Industrial	0.87	0.85
Industrial	1.38	0.60

Additional calibration of the chemical interactions was more recently obtained on a pulp and paper mill wastewater, as shown in Figures 4 and 5. An RQ of 0.90 was used to fit the data. Although the ammonia nitrogen data was scarce, a lower requirement for growth was indicated by the data than typically expected for biological systems. Due to secrecy agreements, only the relative gas phase data are presented.

Model Application to System Design and Analysis

The above model framework is presently used for analysis and design of closed tank high purity oxygen and air aeration systems.

For high purity oxygen systems, the ability to evaluate tradeoffs between mixer horsepower and pure oxygen gas flow (O_2 production horsepower) is shown in Figure 6. In this case, K_La is related to N_o as follows:

$$K_La = \alpha N_o P_V \theta^{(T-20)}/8.34\ C^*_{20} \tag{6}$$

where:

K_La = hr^{-1}

α = dimensionless

N_o = standard aeration efficiency, lb O_2/hp·hr

$\theta^{(T-20)}$ = dimensionless

p_V = power level, hp/mil gal

C^*_{20} = mg/l

For the closed system model, the effect of waste characteristics on transfer rate, as well as temperature effects, must be taken into account as in air aeation systems. The saturation value must also be corrected for depth if submerged turbine or diffused aeration systems are used.

Equation 6 relates the required aeration horsepower to the oxygen transfer coefficient used in the model and provides the ability to evaluate the effect of horsepower split (O_2 generation and aeration) on total power required for all operating conditions. To illustrate application of the model it will be applied to treatment of a wastewater from a chemical plant with the following conditions:

Q = 15.4 mgd

TOD = 1144 mg/l at 24-hr peak load

F/M = 1.58 lb TOD/lb MLVSS·day at 24 hr peak load

TOD = 608 mg/l at average daily load

F/M = 0.84 lb TOD/lb MLVSS·day at average daily load

Alkalinity = 500 mg/l, a' = 0.53, b' = 0.05 day^{-1}

pH = 10.3

Using a 2-1-1 tank configuration and an N_o of 3.0 lb O_2/hp·hr with 80% TOD removal, the design option of the program was utilized to evaluate minimum horsepower required to obtain a desired dissolved oxygen level.

Modern oxygen generation plants require between 0.88 and 1.29 hp/scfm of oxygen.* Curve (a) in Figure 7 is a design curve for the peak load of 1144 mg TOD/l using a power charge of 1.25 hp/scfm for oxygen generation. Portions of the curve at low aeration horsepower correspond to high influent oxygen flow rate. As aeration horsepower is increased, less oxygen flow is required to maintain a DO of 2 mg/l and the percent oxygen utilization represented in curve (b) increases. It is apparent that

*UNOX Brochure

Figure 2. Gas Phase Model Calibration for Batavia, New York Pure Oxygen Treatment Plant

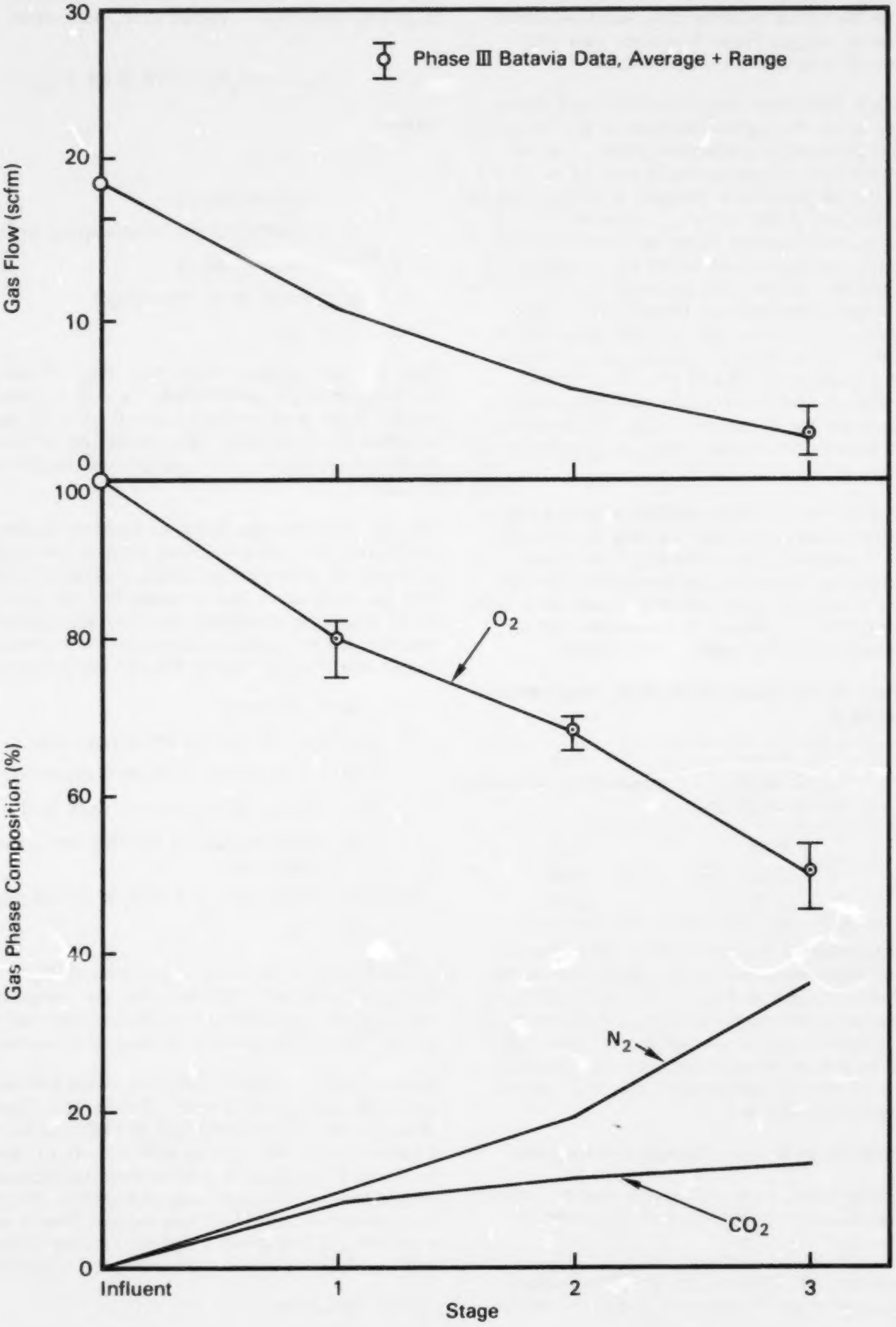


Figure 3. Liquid Phase Model Calibration for Batavia, New York Pure Oxygen Treatment Plant

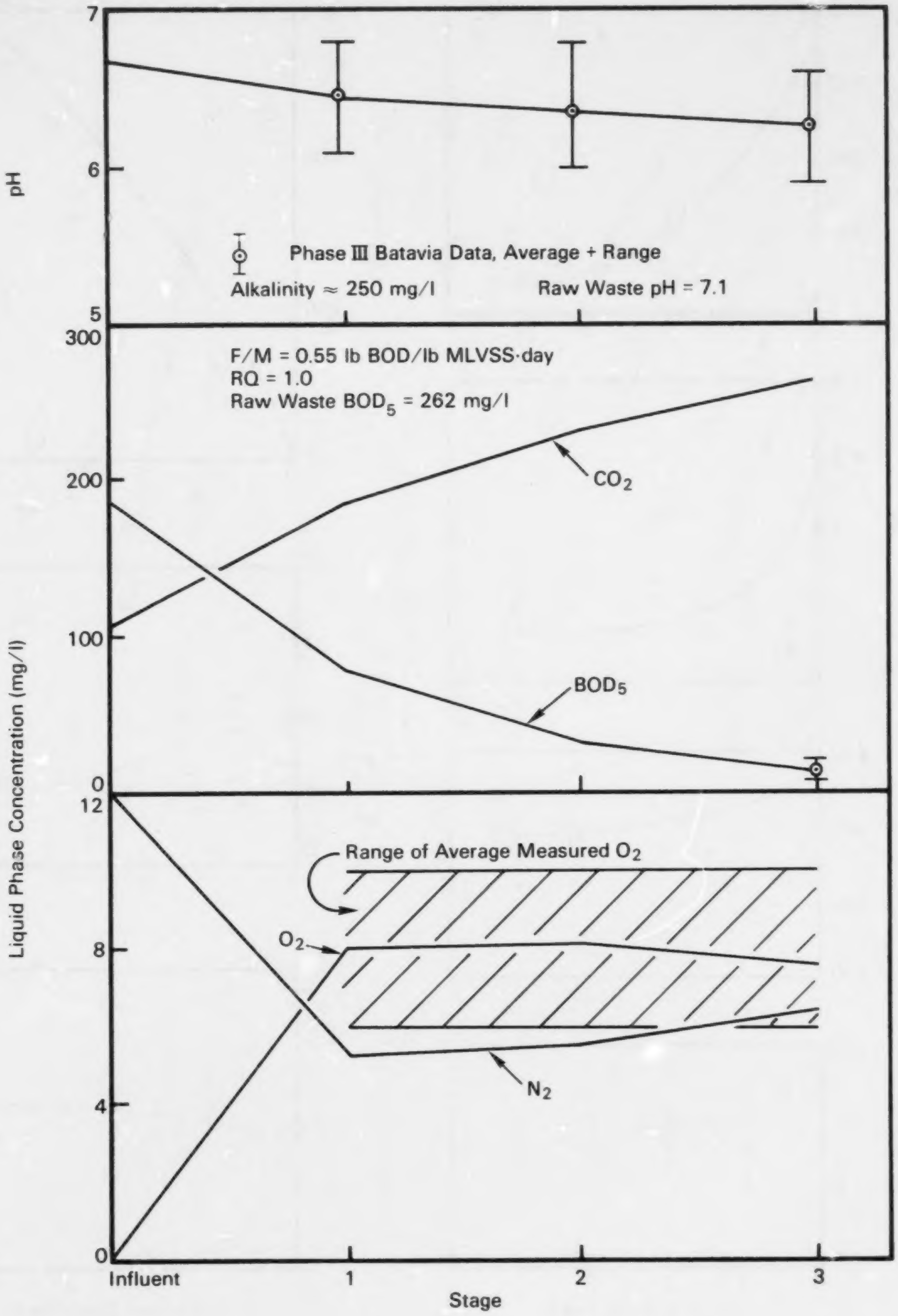


Figure 4. Pulp and Paper Pilot Study Liquid Phase Data High Purity Oxygen Model Calibration

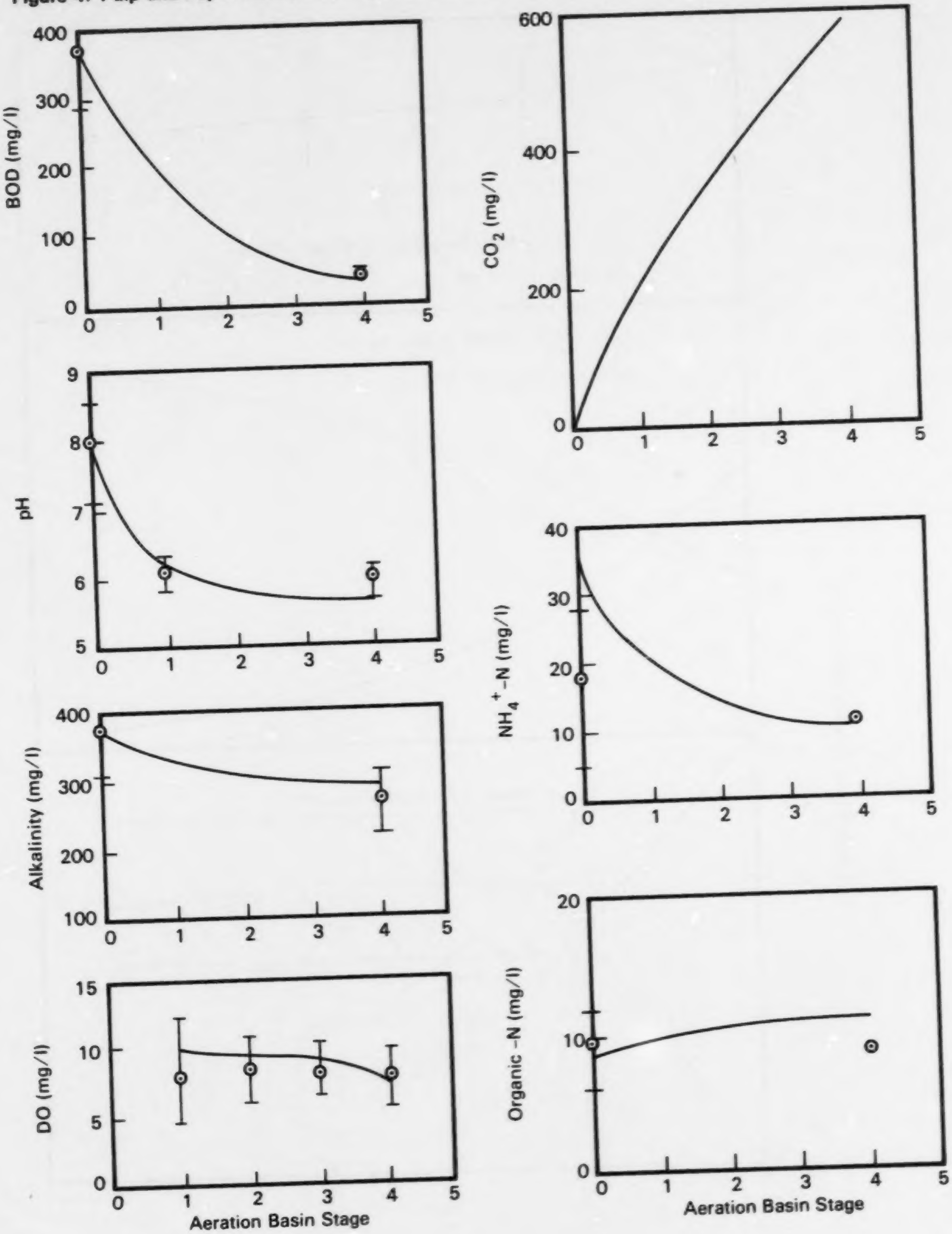


Figure 5. Pulp and Paper Pilot Study Gas Phase Data, High Purity Oxygen Model Calibration

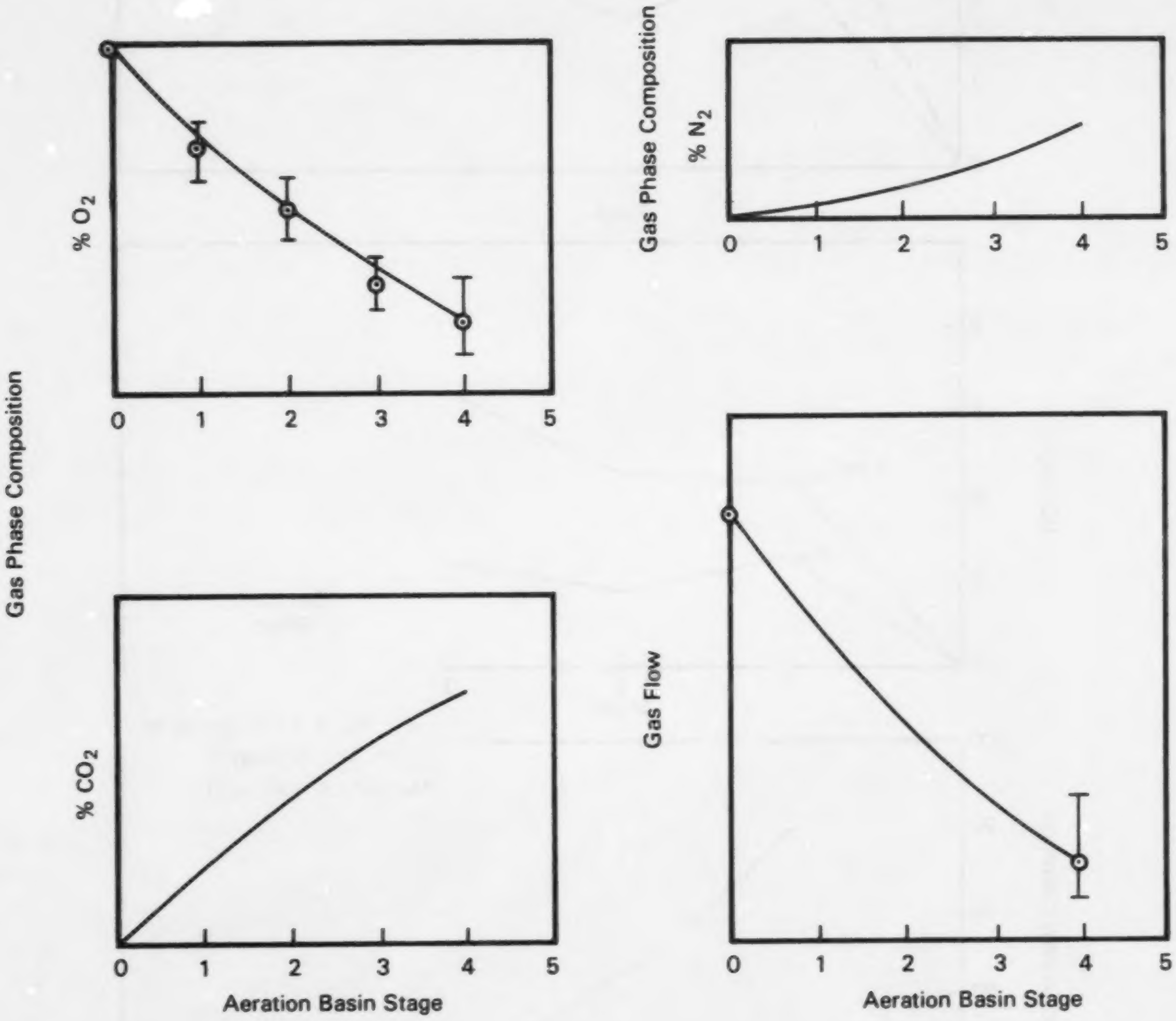


Figure 6. Effect of Rated Capacity and Gas Flow on Dissolved Oxygen Concentration and Oxygen Utilization

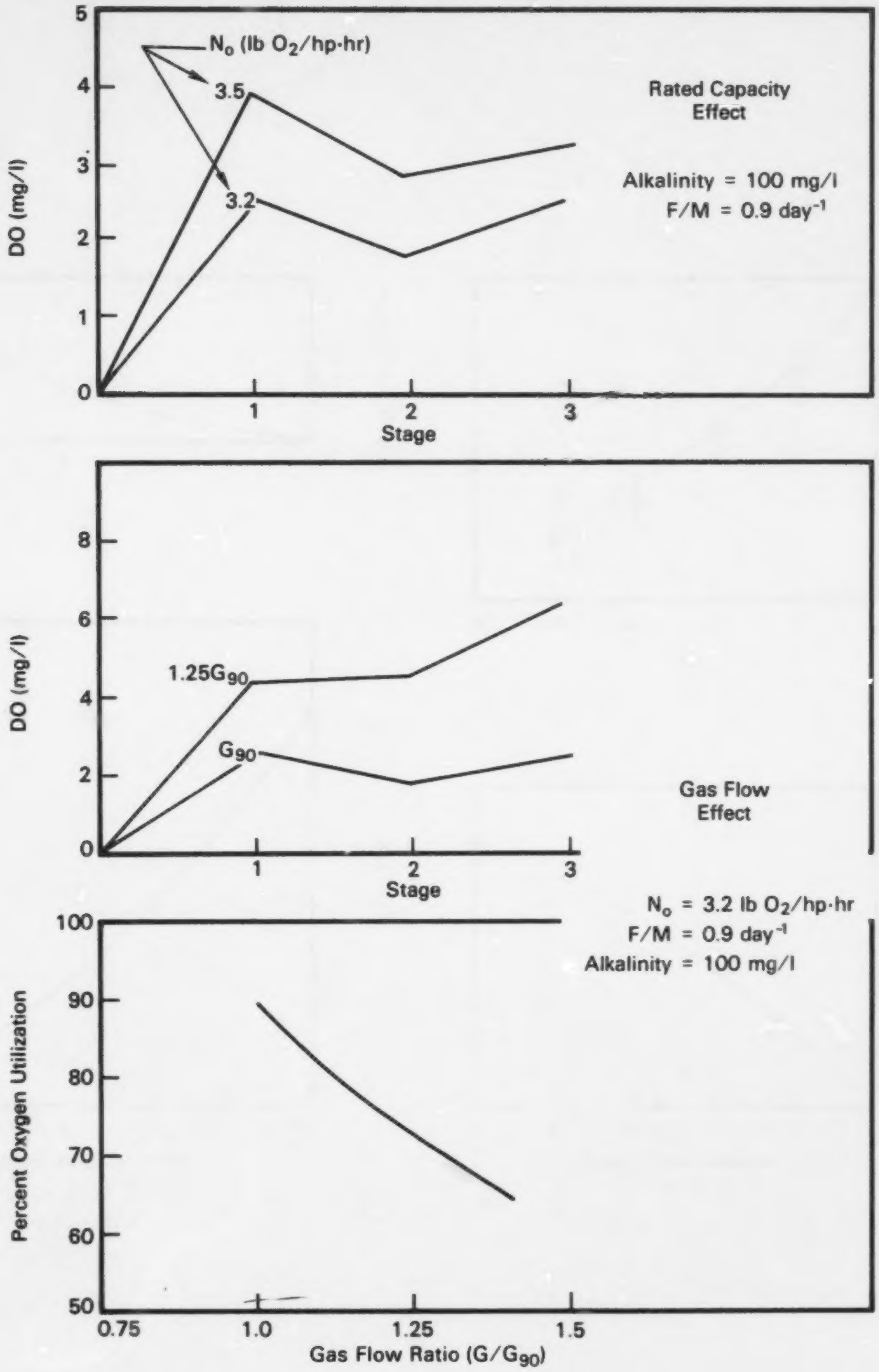
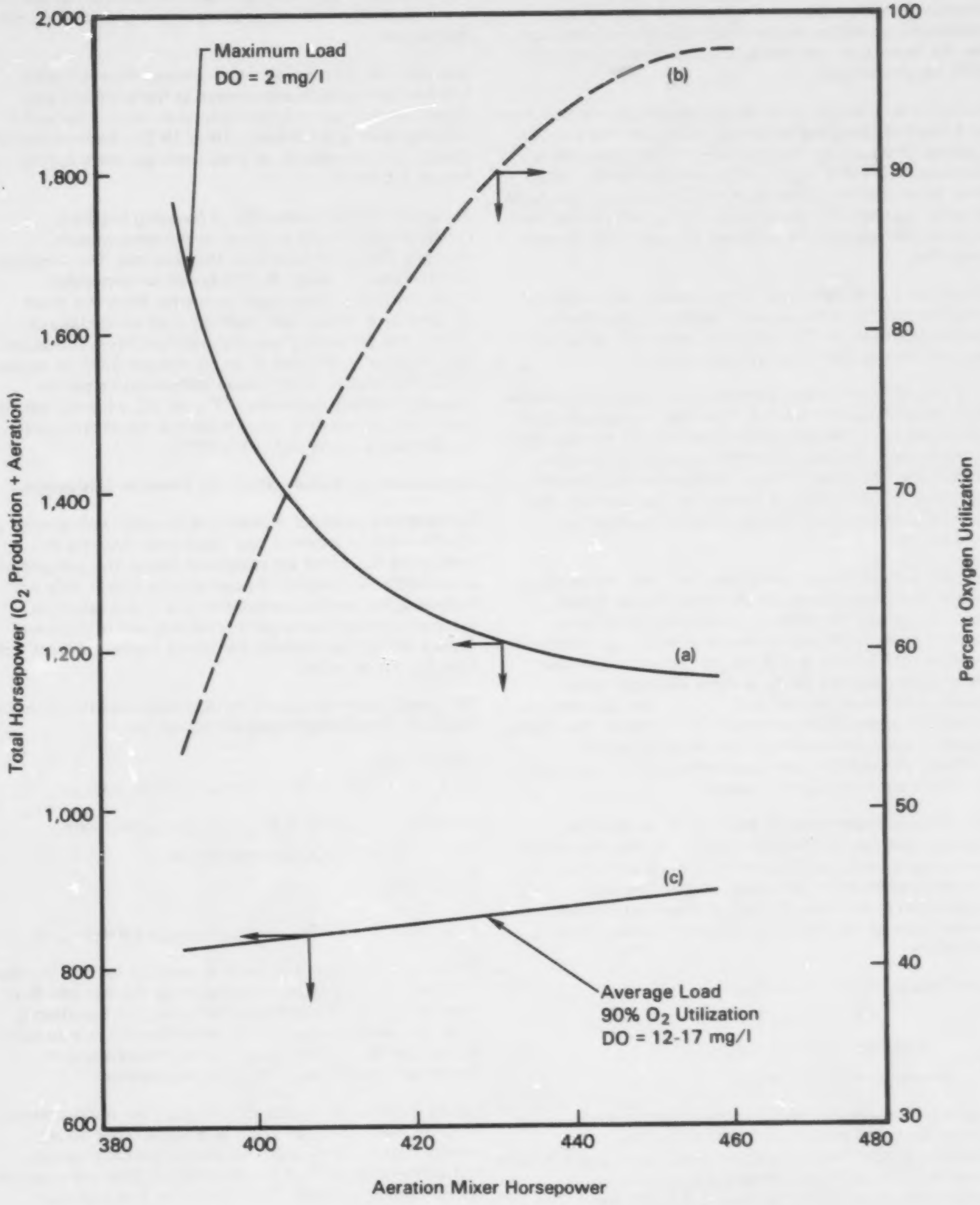


Figure 7. Treatment Power Consumption for Average and Peak Load Conditions



optimum operation corresponds to use of as little oxygen as is possible, a direct consequence of the above power charge. Total horsepower requirements for average conditions, using the same mixer horsepower required for the maximum conditions, are shown as curve (c) for 90% oxygen utilization.

In Figure 8, a design curve corresponding to maintenance of 4 mg/l of dissolved oxygen is shown for the average loading. The aeration horsepower of 250, adequate to maintain a DO of 4 mg/l under average loading conditions is unable to achieve a DO of 2 mg/l during periods of peak loading. An examination of Figure 8 reveals that at least 390 aeration horsepower is required to accomplish this.

Clearly, the peak demands of this system suggest the need for a variable horsepower aeration system which can be operated at 450 hp during peak load conditions and reduced to 250 hp at average loading.

It is desirable, in certain instances, to cover conventional air activated sludge systems. This may be necessary in extremely cold climates to conserve heat in the system in order to maintain good organic removal and if wastewaters contain materials that are volatile and have the tendency to strip from the wastes during aeration, thus, causing potential odor problems to the surrounding community.

As an example, covers were installed over the aeration basins of a conventional air activated sludge system in order to capture the odorous gases that were being stripped from an industrial wastewater during treatment. Two aeration basins of 0.7-mil gal capacity each had three fixed-mounted 75 hp surface aerators. After covering the basin, an exhaust system was installed to collect off gases. Air now enters the basins by the slightly negative pressure created by the exhaust system, primarily through the openings where the aerator impeller shafts pass through the cover.

The exhaust system had to be sized to provide the required gas flow through the basins, so that the necessary oxygen could be transferred to the liquid phase by the existing aerators. The required gas flow was determined to maintain 2 mg/l of dissolved oxygen in the mixed liquor at the following maximum design loading conditions:

Wastewater Flow = 0.54 mgd
COD = 32,000 lb/day
Alkalinity = 2,500 mg/l
Ammonia-N = 900 mg/l

Figure 9 presents the results of these analyses. At gas flows of less than 3,000 scfm, basin dissolved oxygen levels would be less than 1 mg/l. At air flows of 7,500 scfm, the projected dissolved oxygen concentrations would range from 1.5 to 2.0 mg/l. The exhaust fan and duct work to collect the off gases were sized for the 7,500 scfm air flow. The exhaust gases were combined with other plant waste gas streams and incinerated.

Sampling of the covered basin showed that relatively uniform gas composition existed in the enclosed gas space, with oxygen concentrations at various locations ranging from approximately 18 to 19.2%. Basin dissolved oxygen concentrations, at peak loadings, were approximately 1.5 mg/l.

In evaluating the desirability of covering any basin, consideration should be given to the temperature changes that will result from the covering. The temperature increase in many instances will be beneficial; however, it may also cause problems. With the given wastewaters which had relatively high temperatures (30°C), the temperature in the aeration basin increased from high temperatures of approximately 33°C to nearly 42°C. The impact of increased temperatures on the biological activity is shown in Figure 10, where it can be seen that the reaction rate decreased significantly as temperatures increased above 32°C.

Application to Steady State O_2 Transfer Evaluation

For respiring systems, closed tank aeration can provide a significant improvement over open tank systems in evaluating K_La since an additional phase, the gas phase, is available for analysis. If accurate gas phase data are collected, the total O_2 utilization rate in the respiring system is known, accurately providing one of the parameters difficult to evaluate for highly loaded systems with high O_2 uptake rates.

The steady state equations for K_La determination in each stage of a multi-stage covered reactor are:

Liquid Phase

$$K_La = [(C_{1,n}-C_{1,n-1})+r]/t_o/(\beta H_1 p_{1,n}-C_{1,n}) \tag{7}$$

where: $r = a'kX_vS + b'X_v$ = oxygen uptake rate

$t_o = V_n^l/Q$ = detention time

Gas Phase

(8)

$$K_La = (M/V_n^l \ \bar{R}T)(G_{n-1}\,p_{1,n-1}-G_n p_{1,n})/(\beta H_1 p_{1,n}-C_{1,n})$$

Presently, Equation 7 is used to analyze for K_La in closed tank, high purity oxygen systems since the exit gas flow from each stage is not measured for use in Equation 8. Thus, the accuracy of the K_La determination is a function of the accuracy of the oxygen uptake measurement, similar to the evaluation of open tank systems.

To use Equation 8 effectively, the gas flow measurement must be made for each stage of a closed tank series reactor system along with the oxygen partial pressure and concentration. This is presently not done with staged closed reactor systems. If this could be accomplished

Figure 8. Variation of Total Horsepower with Mixer Horsepower to Achieve DO = 4 mg/l at Average Load

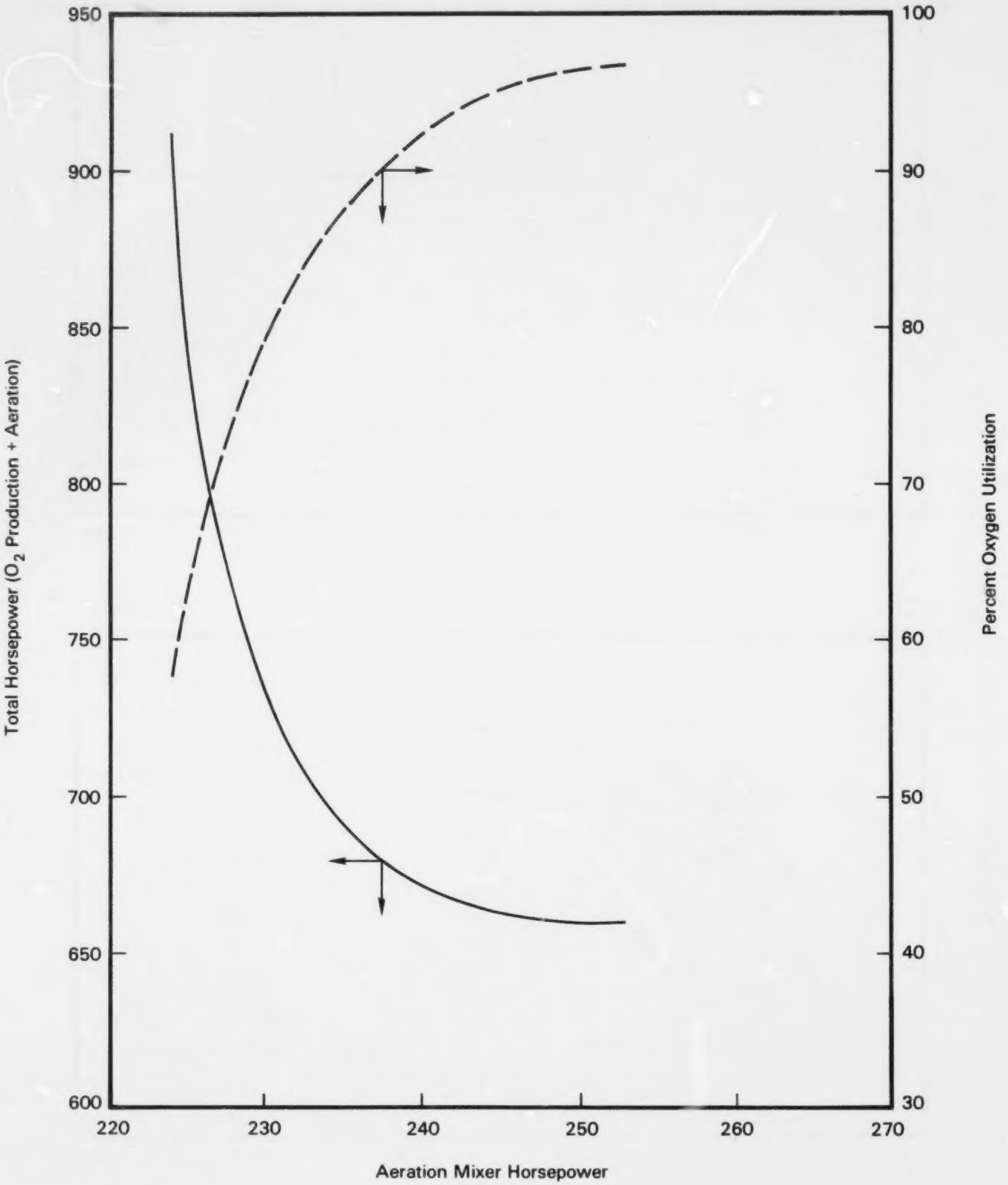


Figure 9. Closed Tank Air Activated Sludge, Gas Flow-Oxygen Relationship

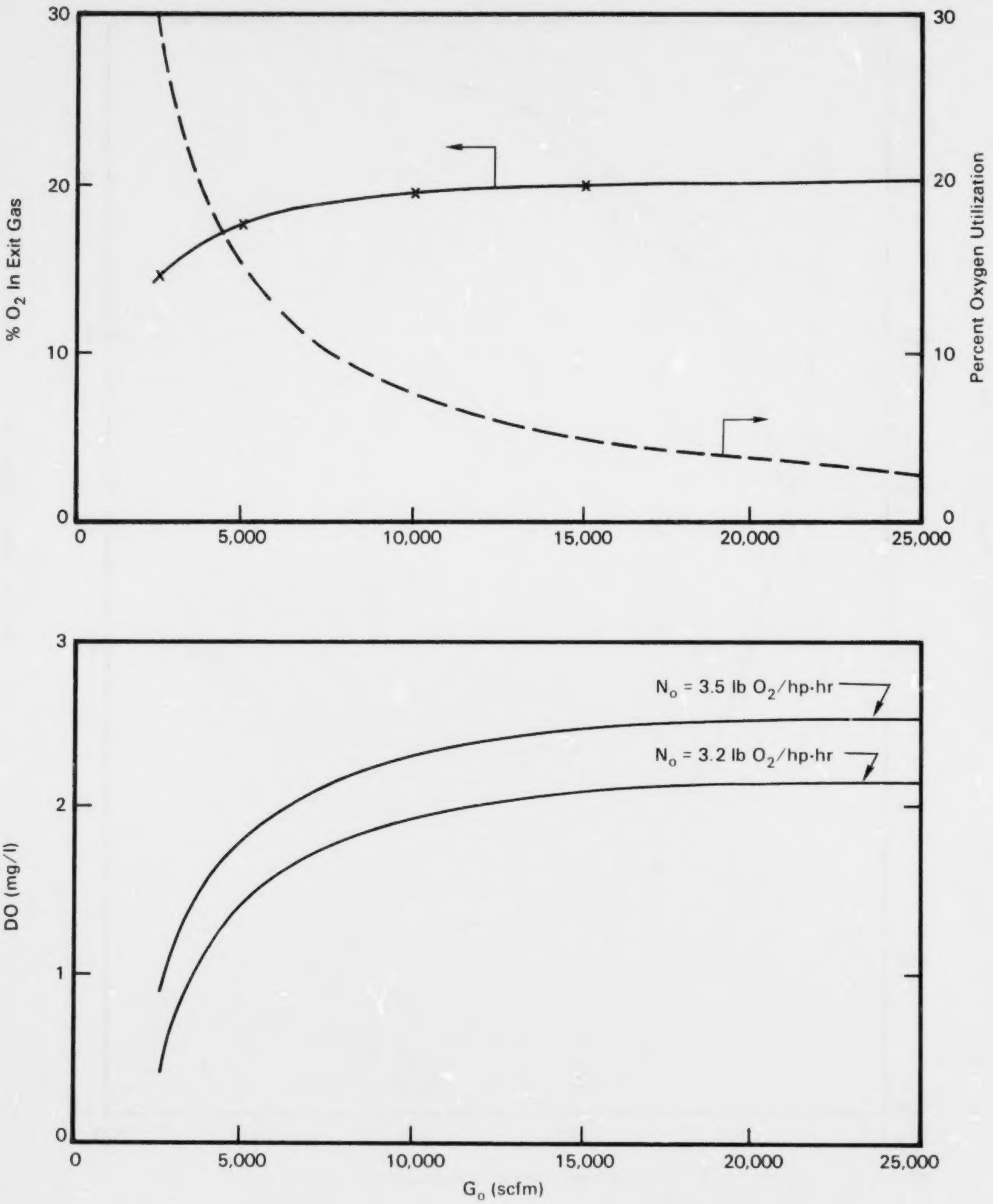
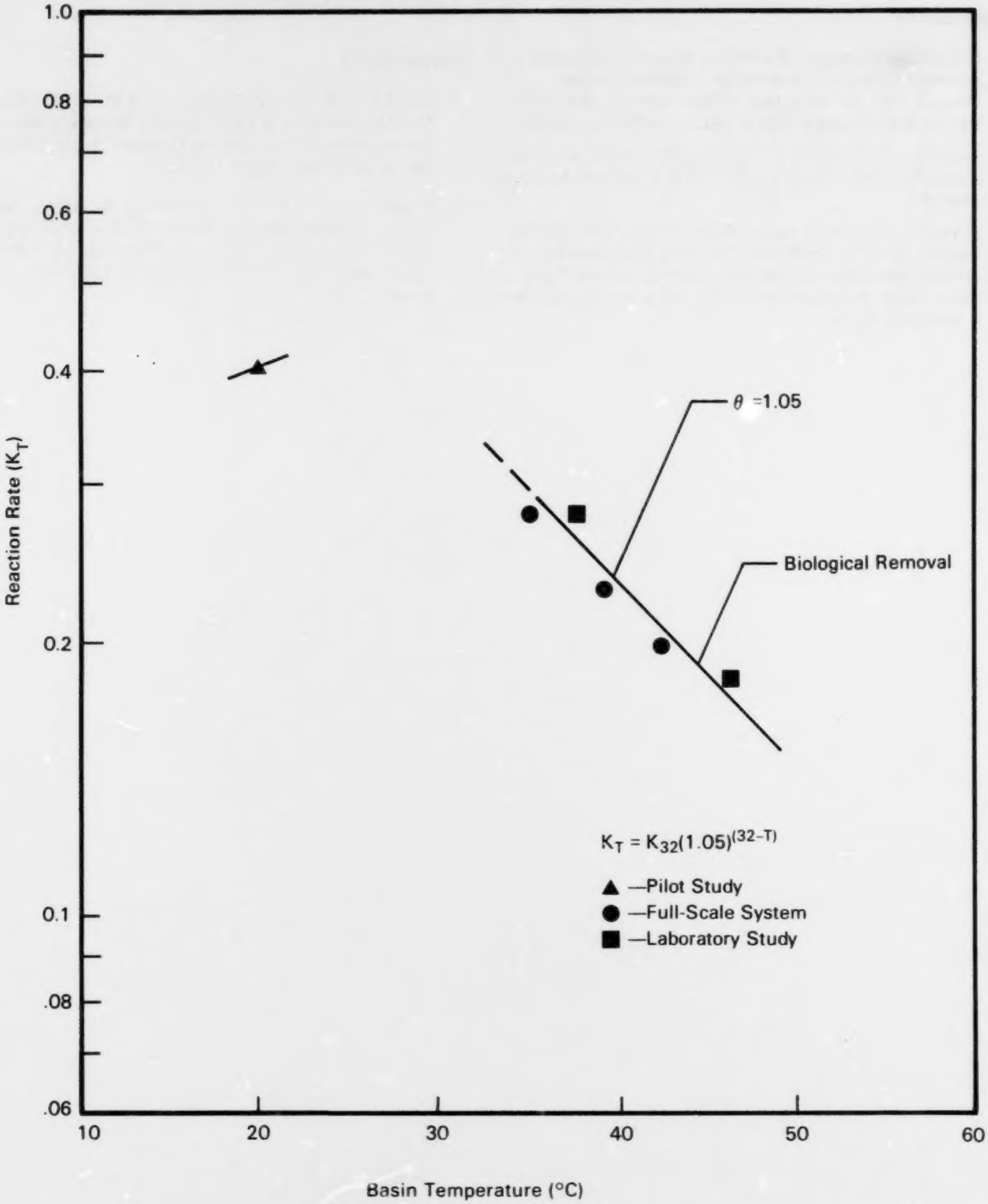


Figure 10. Temperature Effects on Reaction Rate



practically, it would provide an accurate technique for evaluating the transfer capabilities of aeration equipment under actual operating conditions.

Conclusions

1. Analysis and design of aeration equipment requirements for closed tank aeration systems is more difficult than for open tank systems due to the variation of the oxygen partial pressure in the gas phase.
2. A model interrelating gas and liquid phases provides a tool for understanding and properly evaluating these systems.
3. Aeration equipment capabilities, as measured by the oxygen transfer coefficient, K_La, are the same for air and oxygen systems, whether open or closed, for a given piece of equipment at the same power level and system geometry.
4. Closed aeration systems provide an excellent opportunity to accurately evaluate *"in situ"* K_La values using the gas phase mass balance equation. Interstage gas flow measurements are required.

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On Site Evaluation: Steady State Vs Non-Steady State Testing

21

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The overall objective of any aeration system is the transfer of oxygen from the gaseous phase to the microorganisms in the activated sludge. The gaseous phase can be either air or pure oxygen. The oxygen transfer reaction occurs in a series of steps. The first step is transfer from the gaseous phase to the liquid phase. The second step is transfer from the liquid outside the microbial cells to the liquid inside the microbial cells. The non-steady state oxygen transfer test measures only the oxygen transfer from the gaseous phase to the liquid phase, while the steady state oxygen transfer test measures both steps of oxygen transfer. Theoretically, the steady state oxygen transfer test should be preferred to the non-steady state test since it is the only measure of oxygen transfer under actual operating conditions. Unfortunately, the steady state oxygen transfer test is subject to a number of variables which can make evaluation subject to considerable error. For this reason, the steady state test is seldom used anymore for oxygen transfer tests, particularly in the case of manufacturer's acceptance tests.

In response to the efforts of the ASCE to develop an oxygen transfer standard, a study was undertaken to determine whether the steady state test should be included in the standard. Data from a prior test of mechanical surface aerators, where the aerators were tested both by the steady state method in the field and the non-steady state method in the shop, were reviewed and reanalyzed. Steady state tests were then conducted on a full-scale aeration system fitted with mechanical surface aerators, and the results were compared to manufacturer's acceptance tests conducted previously using the non-steady state method. The results of this study and the recommendations to the ASCE committee are presented herein.

Theoretical Relationships

The oxygen transfer relationships in the steady state test differ from those of the non-steady state test in two ways. First, oxygen is not being transferred to clean water; but rather, is being transferred to water containing varying concentrations of contaminants, both organic and inorganic. The contaminants in the test water can affect both the rate of oxygen transfer and the saturation concentration. Surface active organic compounds tend to have the greatest affect on the oxygen transfer relationships. Secondly, in the steady state test the activated sludge creates a continuous oxygen uptake from the water phase. The oxygen transfer equation for the steady state test is shown in Equation 1.

$$dC/dt = \alpha K_L a(\beta C^* - C) - R \qquad (1)$$

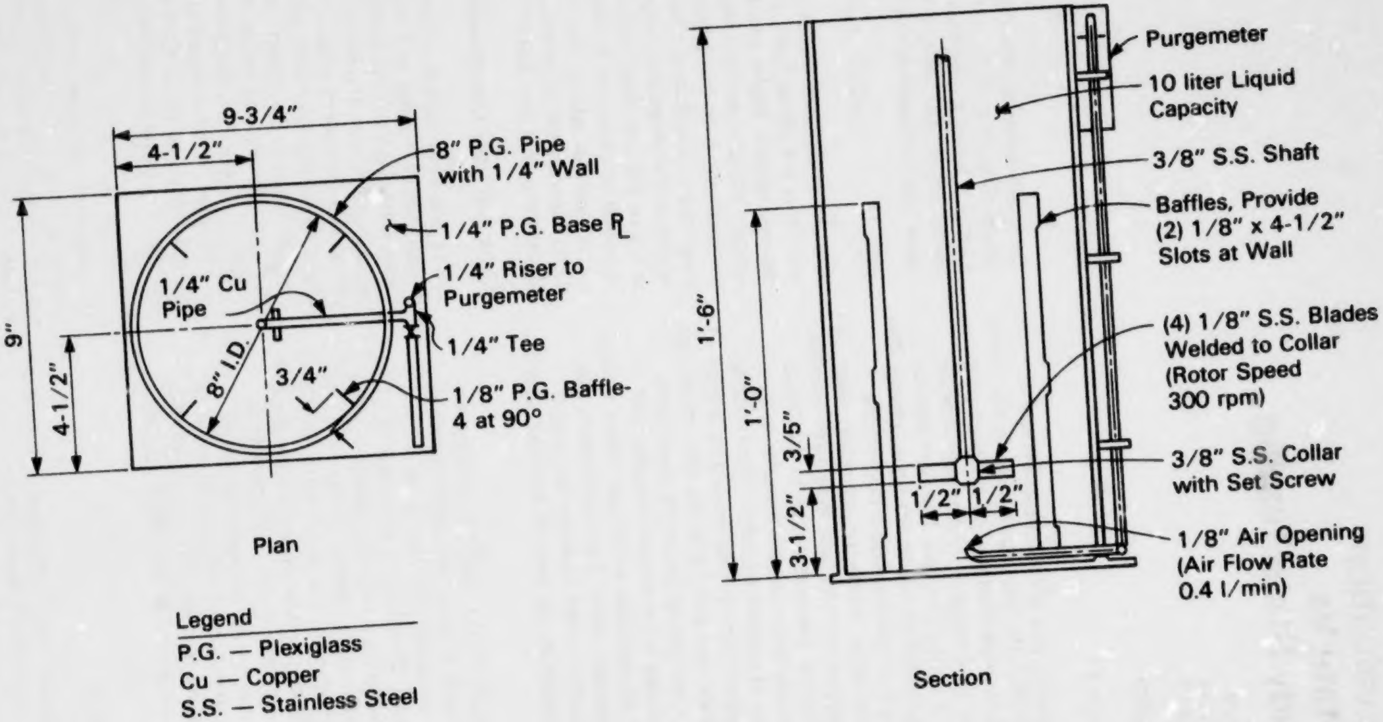
Under steady state conditions, $dC/dt = 0$. The important variables are alpha, beta, dissolved oxygen and microbial oxygen uptake. Alpha and beta are used to correct the oxygen transfer and the oxygen saturation values, respectively, from the actual test water conditions to clean water conditions. One of the most critical measurements is the dissolved oxygen concentration. The problem is not in the dissolved oxygen measurement, but rather in which value to use in the evaluation equation. The oxygen uptake rate by the activated sludge is the most difficult parameter to measure and has been the source of problems in a number of field tests.

Alpha and Beta Determinations

Alpha and beta determinations are made by comparing oxygen transfer rates and oxygen saturation values in clean water against the water in the mixed liquor. A test apparatus such as shown in Figure 1 can be used to collect the necessary data. This unit employs a variable speed mixer together with a controlled flow of air or oxygen. Since the aeration equipment is evaluated at 20°C, it is possible to adjust the test fluid to 20°C with a minimum of effort so that temperature can be eliminated as a variable. If the temperatures of the test fluids cannot be easily adjusted to 20°C, they should be allowed to adjust to the same temperature to minimize temperature effects.

The important variables in the alpha determinations are mixing, air flow rate and temperature. Eckenfelder and Ford (1) indicated that alpha values varied with mixing, making it difficult to determine alpha accurately. The mixing effect can be minimized by operating the test unit at essentially the same K_La that the aeration system was designed to achieve. It will be necessary to run a series of tests on clean water to determine the desired mixing speed and air flow rate. Once the mixing and air flow

Figure 1. Bench-Scale Aeration Tank


9-3/4"
4-1/2"
8" P.G. Pipe with 1/4" Wall
1/4" P.G. Base ℞
1/4" Riser to Purgemeter
1/4" Cu Pipe
1/4" Tee
9"
4-1/2"
8" I.D.
3/4"
1/8" P.G. Baffle-4 at 90°
Legend
P.G. — Plexiglass
Cu — Copper
S.S. — Stainless Steel
Purgemeter
10 liter Liquid Capacity
3/8" S.S. Shaft
Baffles, Provide (2) 1/8" x 4-1/2" Slots at Wall
1'-6"
1'-0"
3/5"
3-1/2"
1/2"
1/2"
(4) 1/8" S.S. Blades Welded to Collar (Rotor Speed 300 rpm)
3/8" S.S. Collar with Set Screw
1/8" Air Opening (Air Flow Rate 0.4 l/min)

Plan

Section

rate have been selected, they should remain constant throughout the rest of the study.

Deoxygenation of the test water can be accomplished by using nitrogen gas to strip the oxygen, or by using sodium sulfite and cobalt chloride to chemically remove the oxygen. Dissolved oxygen measurements can easily be made with a direct reading dissolved oxygen probe. The reaeration data are normally collected from zero oxygen concentration to near saturation at constant time increments selected to give a reasonable change in oxygen between readings. As the dissolved oxygen concentration approaches saturation, the rate of change in concentration is so low that the error in reading the meter will exceed the actual change in concentration making additional data collection meaningless.

Oxygen saturation can be determined directly or measured from the data itself. While long term aeration can produce saturation, care must be taken to insure that the temperature does not increase. Incorrect data can be obtained if test conditions are not held constant. A plot of dC/dt on the ordinate axis vs C on the abscissa (2) should yield a straight line which passes through C^* at $dC/dt = 0$. Further, the slope of the line is K_La, making it unnecessary to develop the semilog plot of (C^*-C) vs t. In addition, the dC/dt vs C plot clearly shows the degree of mixing in the test vessel.

The test water from the aeration tank should be taken at the same time that oxygen uptake measurements are made so that the two measurements can be correlated. A mixed liquor sample should be taken and allowed to settle and the supernatant removed for testing. In many systems it is possible to use the final effluent for the test water if tests are made when the activated sludge system is working well. There is little difference in the chemical characteristics of the liquid in the aeration tank and in the final sedimentation tank if complete mixing occurs in the aeration system. In large aeration tanks, the rate of mixing is such that a gradient can exist from the inlet to the outlet. For this reason it may be necessary to determine alpha and beta values at several points in the aeration system.

The activated sludge-free supernatant is deoxygenated by the same procedure used for deoxygenating the tap water. The oxygen transfer characteristics are determined under the same conditions as for the tap water. Alpha is calculated from the ratio of K_La of supernatant/K_La of tap water. Beta is calculated from the ratio of C^* of supernatant/C^* of tap water. If data are collected at different temperatures, it will be necessary to correct the results to a common temperature base.

Oxygen Uptake Measurement

Oxygen uptake measurements must be made with care if meaningful data are to be obtained. It should be recognized that the sample of activated sludge being analyzed is continuously changing, making it necessary to obtain measurements as quickly as possible at the aeration basin. Since fresh wastes cannot enter the sample continuously, the rate of oxygen uptake decreases and the organics are metabolized to the point that endogenous respiration is the sole cause of the oxygen uptake. Since endogenous respiration is relatively constant, it gives an apparent steady oxygen uptake rate that is often mistaken for the overall oxygen uptake rate in the aeration basin.

As a result of the problems in obtaining the real oxygen uptake rate in the aeration basin, it has been suggested that the aeration tests be run under endogenous respiration conditions (2). By stopping the waste flow to the aeration basin being tested and continuing the sludge recirculation system, the activated sludge will quickly reach endogenous metabolism and will have a uniform oxygen demand rate over the aeration system. The rate of change in endogenous respiration is slow enough that measurements can be made with less chance for error. Since the oxygen respiration rate will be relatively low, the dissolved oxygen in the aeration basin will be high, making it very easy to take a sample and determine the oxygen uptake rate without any additional aeration. If the initial dissolved oxygen level of the sample is low, it will be necessary to add oxygen prior to determining the oxygen uptake rate.

In the activated sludge systems that are lightly loaded, the rate of endogenous respiration will be too low for proper evaluation. Under these conditions, it will be necessary to test the aeration system under normal loading conditions. The rate of oxygen uptake will usually still be low enough that it can be measured without too much difficulty. Experience indicates that oxygen uptake rates under 60 mg/l·hr can be measured with a minimum of error.

The oxygen uptake data can easily be taken with a direct reading dissolved oxygen probe. The sample of mixed liquor must be continuously stirred to keep the solids in suspension and dispersed so that oxygen is available to all the microbes. Stirring can be accomplished with a magnetic stirrer, a mechanical mixer or simply by mixing with the DO probe itself. As the dissolved oxygen concentration decreases, a point will be reached where the rate of oxygen uptake will slow down. This slowing in oxygen uptake generally occurs when oxygen becomes limiting in the test system and has no relationship to the full-scale aeration tank. A plot of the oxygen uptake data vs time will show when oxygen becomes limiting in the test.

Dissolved Oxygen Measurements

Aeration test data are meaningful only if the entire aeration tank has excess dissolved oxygen at all points. Testing during endogenous respiration generally insures adequate dissolved oxygen, while testing during feeding of wastes can result in some points within the aeration tank not having adequate dissolved oxygen. The important dissolved oxygen concentration in the aeration tank is the DO in the liquid approaching the aerator since that value determines the driving force across the aerator. Since aerators transfer oxygen primarily at a point, the dissolved oxygen concentrations vary around the aeration tank, depending upon the overall pattern of fluid flow in the aeration tank. Determining the dissolved oxygen profile around the aeration tank can be quite helpful in understanding the fluid flow patterns in the aeration tank. It can easily be determined if the fluid is short-circuiting around the aeration tank or flowing as expected.

K_La Calculations

Equation 1 can be used to calculate K_La at the test temperature. It will be necessary to correct the K_La to 20°C as shown by Equation 2.

$$(K_La)_{20} = (K_La)_T\theta^{(20-T)} \quad (2)$$

The value of θ has largely been determined experimentally, with the value of 1.024 generally being accepted. However, examination of K_La indicates that the oxygen transfer to the water and the mixing of the oxygenated water internally is controlled by the dynamic viscosity of the liquid. It would appear that the relative ratio of dynamic viscosities of water at 20°C and the test temperature could be a better correction factor than an empirical factor determined from a limited set of data.

Results from Full-Scale Testing

Midland, Texas

The specified manufacturer's acceptance test for the Midland, Texas, aeration equipment was the steady state method. However, when differences arose between the manufacturer and the engineer in evaluating the data, non-steady state tests were conducted on a similar aerator in a shop test tank.

The Midland aeration basins are 76 ft in diameter with a 16 ft side water depth. Each basin is equipped with four, 50 hp slow speed surface aerators mounted above draft tubes. A total of 13 steady state tests were conducted over a 4-day period in November 1974. Dissolved oxygen measurements were made with a direct reading dissolved oxygen probe. Each test consisted of three oxygen uptake measurements, 13 DO measurements in the aeration basin and alpha and beta measurements. The 13 DO measurements in the aeration basin consisted of 3 sets of 4 measurements at 1.5, 4, 8 and 14.5 ft below the surface and one measurement in the middle of the draft tube of one of the aerators. The test specifications permitted averaging all of the DO measurements rather than using just the DO value in the draft tube. This procedure would have resulted in high values for K_La if the oxygen uptake data had been properly taken. Since some time elapsed between the time mixed liquor samples were taken and uptake rates were measured, the measured uptake rates were probably lower than the actual values in the aeration basin. Overall, the two errors resulted in a $(K_La)_{20}$ value of 7.2 hr^{-1}, considerably less than the specified value of 13.0 hr^{-1}. In order to meet the specification, it was suggested by the manufacturer that K_La varied with oxygen uptake and that extrapolation of the data would yield the desired value. Since K_La had not been shown to vary with oxygen uptake rates, this concept was rejected and the aerator was subjected to a non-steady state test.

The non-steady state test was carried out in a square tank with the aerator in the center. The total volume of fluid was essentially the same as the volume influenced by a single aerator in the aeration basin. Five points were selected for dissolved oxygen measurements with two points 3 ft below the surface, two points 2 ft off the bottom and one point under the draft tube. The data showed the affect of the tank configuration, giving a surge pattern as shown in Figure 2. The surge which was noted at an upper sample point was damped out at the sample point beneath the draft tube, but the relative variations were similar. A total of three replicate tests were made, yielding a $(K_La)_{20}$ of 15.6 hr^{-1} as the average of 15 data points. The K_La values were determined from three-point running averages (denoted by solid triangles on the figure) using the dC/dt vs C curves. The conventional semilog plots yielded an average $(K_La)_{20}$ of 13.4 hr^{-1}. The non-steady state test clearly indicated that the aerator was capable of meeting the specified $(K_La)_{20}$ value.

The differences in the results prompted reexamination of the steady state data. The data were examined on the basis of dC/dt vs C plots rather than the simple calculations of K_La. Theory indicated that a plot of $(dC/dt)/\alpha$ vs C should yield a straight line passing through C^* at $(dC/dt)/\alpha = 0$. Examination of three sets of data at different oxygen uptake rates on three different days indicated that these data points were on a straight-line, but that line did not intercept the abscissa at the measured values of C^*. The data indicated that the oxygen uptake rates were low. A plot of a line through the data and the measured C^* gave a theoretical line that should have corresponded to the actual oxygen uptake rates divided by alpha. The $(K_La)_{20}$ from this line was 16.2 hr^{-1}, very similar to non-steady state results. These results clearly pointed out the problems in steady state testing.

Lawrence, Kansas

The principal problem with the Midland, Texas, steady state test results was the apparently low oxygen uptake values. To determine if this error could be minimized, steady state tests were performed on all the aeration equipment in one aeration basin at the Lawrence, Kansas, wastewater treatment plant. This same equipment had been tested earlier in the aeration basin by the non-steady state test as a part of the manufacturer's acceptance tests. Thus, an excellent opportunity was provided for comparison of steady state vs non-steady state test procedures and results.

Non-Steady State Testing

The Lawrence aeration basins are 105 ft in diameter with a 14 ft side water depth. Each aeration basin is equipped with four 60 hp surface mechanical aerators mounted over draft tubes. Non-steady state testing was conducted in May 1976 with direct reading dissolved oxygen probes located at five points in the basin. The signal from each probe was recorded on a continuous graph so that it was possible to examine the surges in mixing within the aeration basin. These data were very carefully reevaluated for this paper to provide a base for comparison with the results from steady state testing. The analysis of the non-steady state data indicated that a $(K\ a)_{20}$ of 9.4 hr^{-1} was obtained at 51.6 motor shaft hp. The dC/dt vs C plot for the DO probe located below the draft tubes is shown in Figure 3. This point was selected since the continuous graph showed definite surging in the dissolved oxygen data. The shifts in the dC/dt reflect the surges caused by the irregular shape of the tank volume affecting this aerator. Figure 3 also shows the conventional semilog plot of these same data. The semilog plot definitely dampens out the variations in the data and yields a reasonable set of data that can be easily evaluated. The dC/dt vs C plot was evaluated using a three-point running average and linear regression analysis. The semilog plot was evaluated by the equipment manufacturer using their computer program and yielded a $(K_La)_{20}$ of 9.3 hr^{-1}. Essentially, both methods yielded the same results, but the dC/dt plot showed the surging that occurred in the aeration tank.

Steady State Testing

Steady state testing was conducted at Lawrence on February 3, 1978. The testing was performed in the same basin used for non-steady state testing. Power measurements were made on the same aerator monitored during the non-steady state tests. Two steady state tests were conducted, one in the morning and one in the afternoon. At the conclusion of the morning tests, it was discovered that the DO meter used for α, β and dO/dt measurements were incorrectly calibrated. However, all measurements were off by a constant factor. Therefore, the data are valid and have been included in this paper.

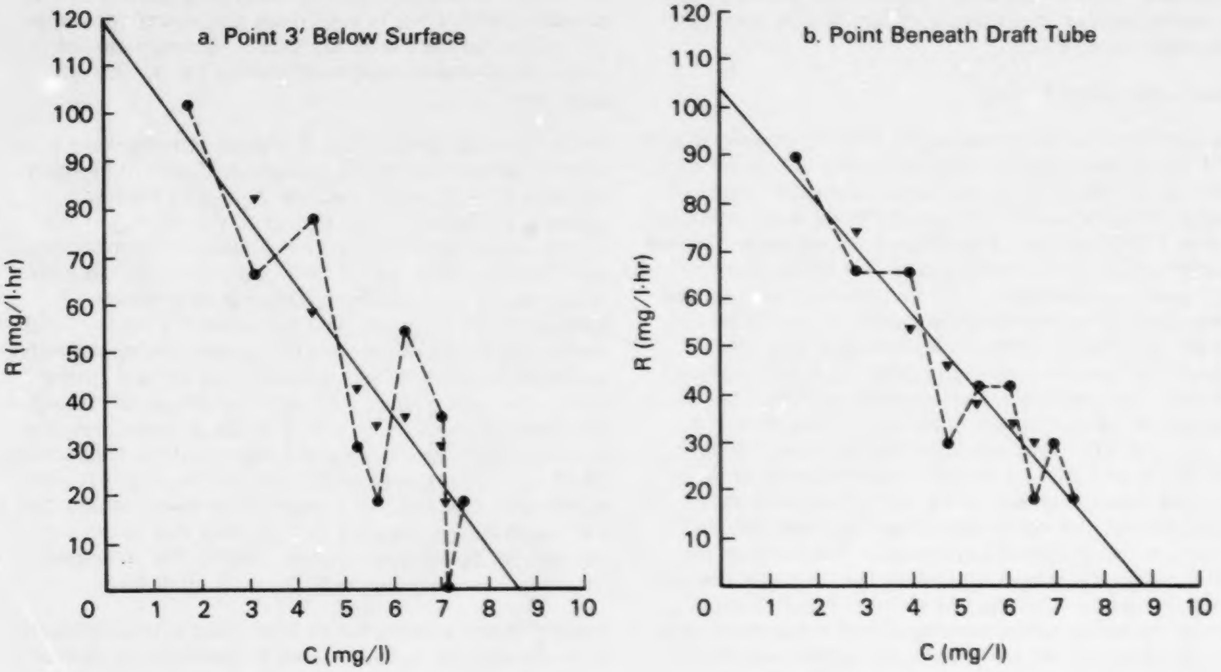
The morning tests were conducted on mixed liquor which had been allowed to enter into endogenous respiration. Wastewater flow to the aeration basin was discontinued 2 hr prior to testing to insure oxidation of all the residual organic matter from the wastewater. After the morning tests had been completed, wastewater flow to the aeration basin was reestablished. The afternoon tests were conducted on mixed liquor that had been actively metabolizing wastewater for approximately 1-1/2 hr. In both tests, the sludge recycle pumps were left in operation to return sludge from the secondary clarifier to the aeration basin. Also, in both tests, the power consumption of the aerators was adjusted to approximate the power consumption measured during the non-steady state tests.

In the morning tests, α and β measurements were made on final effluent using the apparatus shown in Figure 1. Conditions were set so that the $(K_La)_{20}$ of the test apparatus (14.0 hr^{-1}) was similar to the $(K_La)_{20}$ of the aeration basin (9.4 hr^{-1}). Figure 4 shows the uncorrected data used for the α and β determinations, plotted both by the dC/dt vs C and by the log deficit methods. Although both methods yield the same results, $\alpha = 0.82$ and $\beta = 0.98$, the variations in the data are more readily apparent from the dC/dt vs C plot. The oxygen uptake rate of the mixed liquor, corrected for the probe calibration error, was 13.6 mg/l·hr. The mixed liquor temperature was 9.6°C at the time of testing and the MLSS were 1810 mg/l. The corrected DO concentration at the inlet to the draft tube was 10.1 mg/l. From these values, the estimated $(K_La)_{20}$ was 9.1 hr^{-1}, comparable to the value determined by non-steady state testing. The measured aerator power output was 52.2 motor shaft hp.

Testing in the afternoon was conducted with wastewater flow through the aeration basin to increase the oxygen uptake rate above the relatively low endogenous respiration rate. Tilting weirs were adjusted with the initiation of flow through the basin so as to keep the power consumption of the aerators the same as that used in the morning tests. The problems with the DO probes were corrected, and clarifier effluent was sampled for α and β determinations. The α and β data are plotted in Figure 5 using the dC/dt vs C plot.

The long aeration period without wastewater resulted in a change in alpha to 0.97. The beta value remained essentially the same at 0.97. The K_La values for the alpha and beta determinations were again somewhat higher than the full-scale unit, but it was not felt the difference was significant. The DO profile in the aeration tank, shown in Figure 6, gave a mixing pattern which was typical for this type of unit. The fluid from the aerator appeared to move in a thin layer across the surface to the wall and down to the bottom with normal intermix in the middle zone. The minimum DO was estimated to be 7.3 mg/l from the data at the bottom of the tank next to the draft tube. The range of DO values in the aeration tank definitely shows that the average DO is not related to the oxygen transfer, except indirectly. The $(K_La)_{20}$ was calculated as 9.1 hr^{-1}. The $(K_La)_{20}$ value from the afternoon steady state test was essentially the same as the results from non-steady state testing, within the normal error range for such tests.

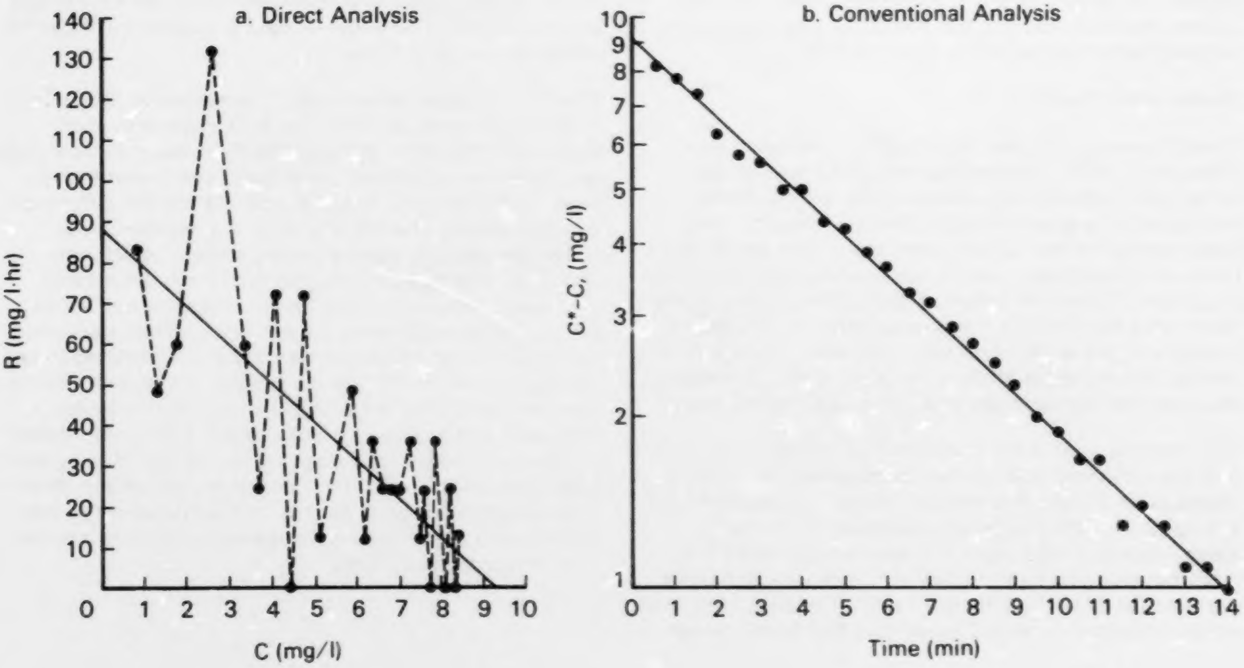
Figure 2. Non-Steady State Test Data, Midland, Texas



a. Point 3′ Below Surface

b. Point Beneath Draft Tube

Figure 3. Non-Steady State Test Data, Lawrence, Kansas Sample Point Under Draft Tube



a. Direct Analysis

b. Conventional Analysis

Figure 4. Morning Alpha and Beta Test Data Steady State Test at Lawrence, Kansas

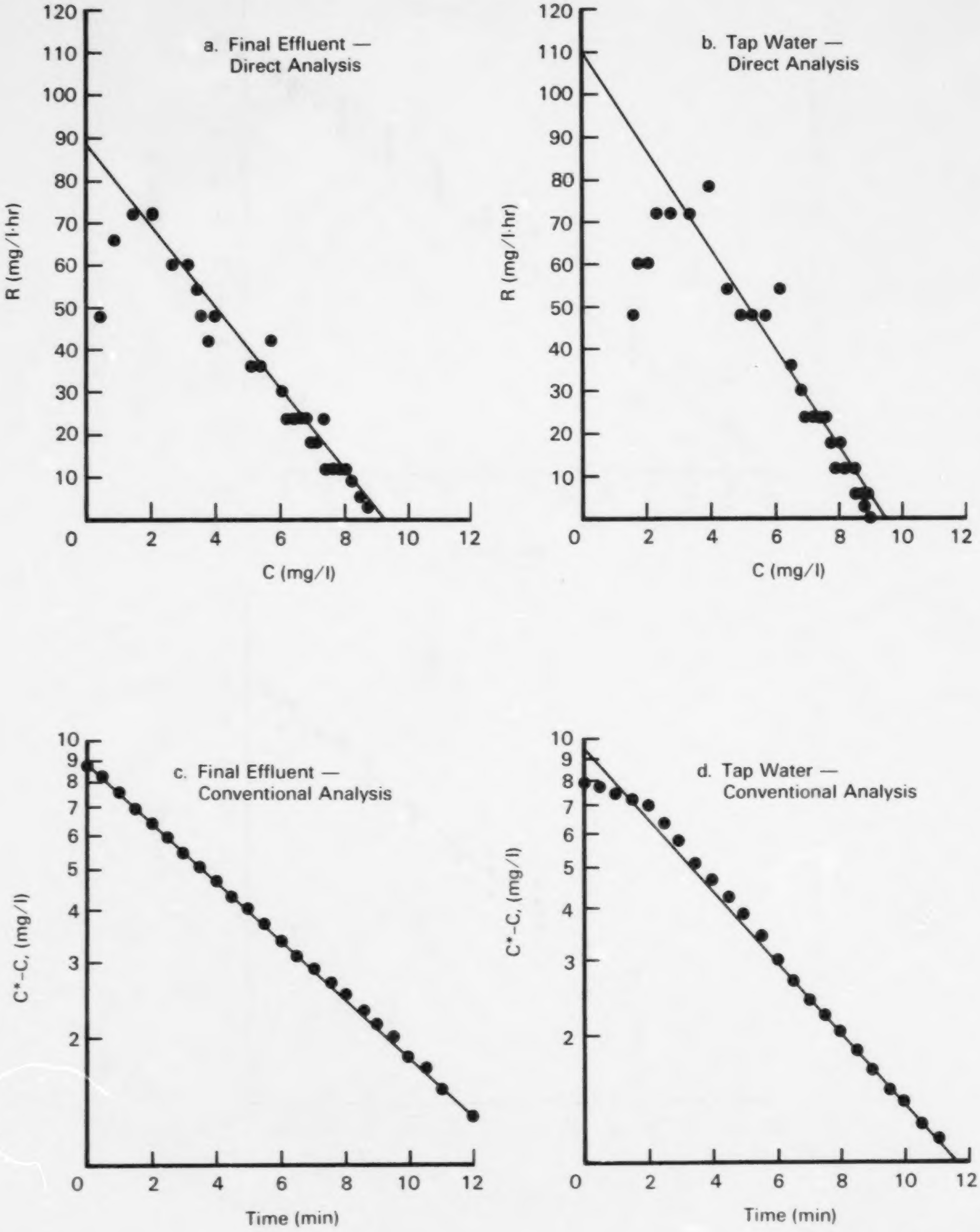
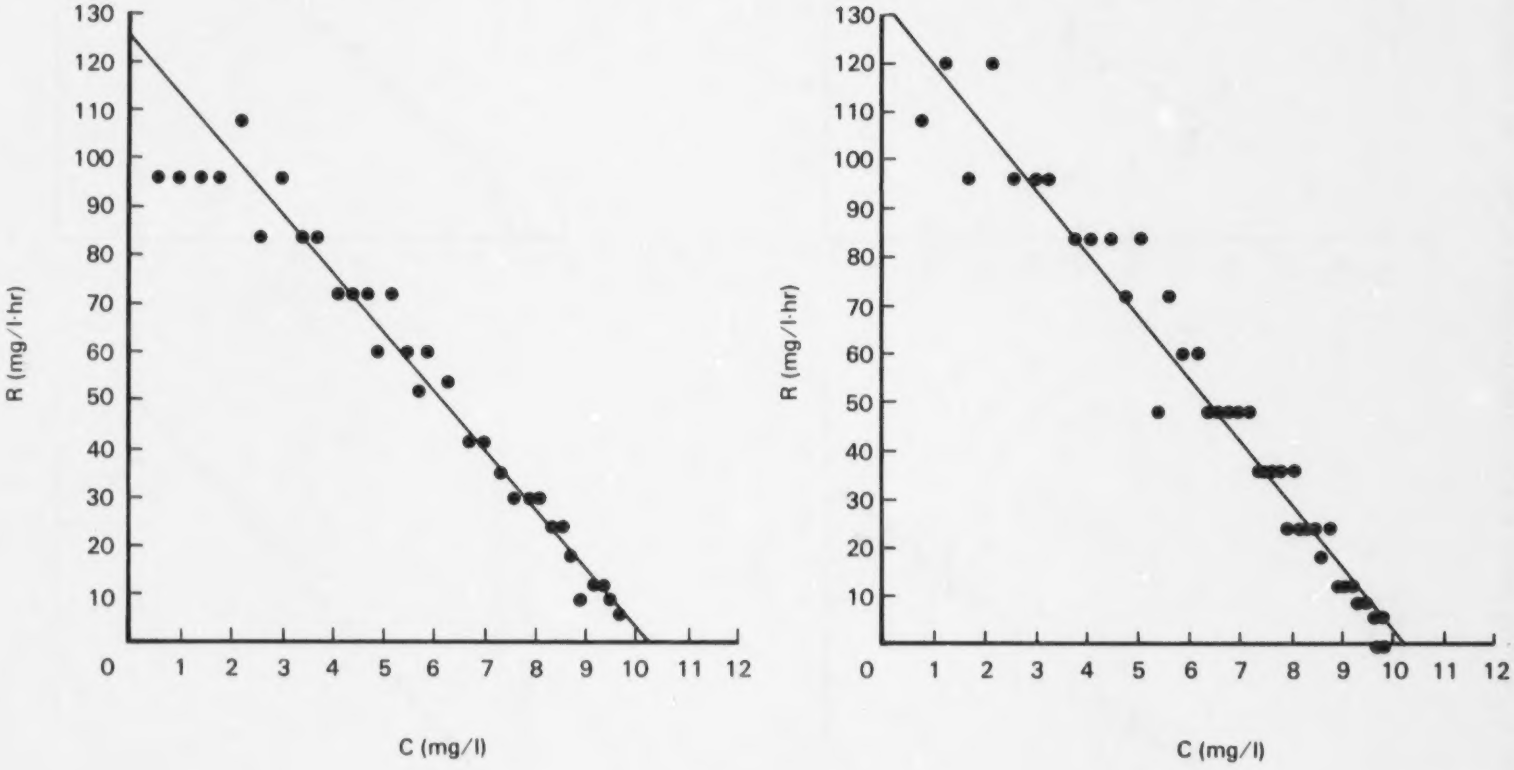


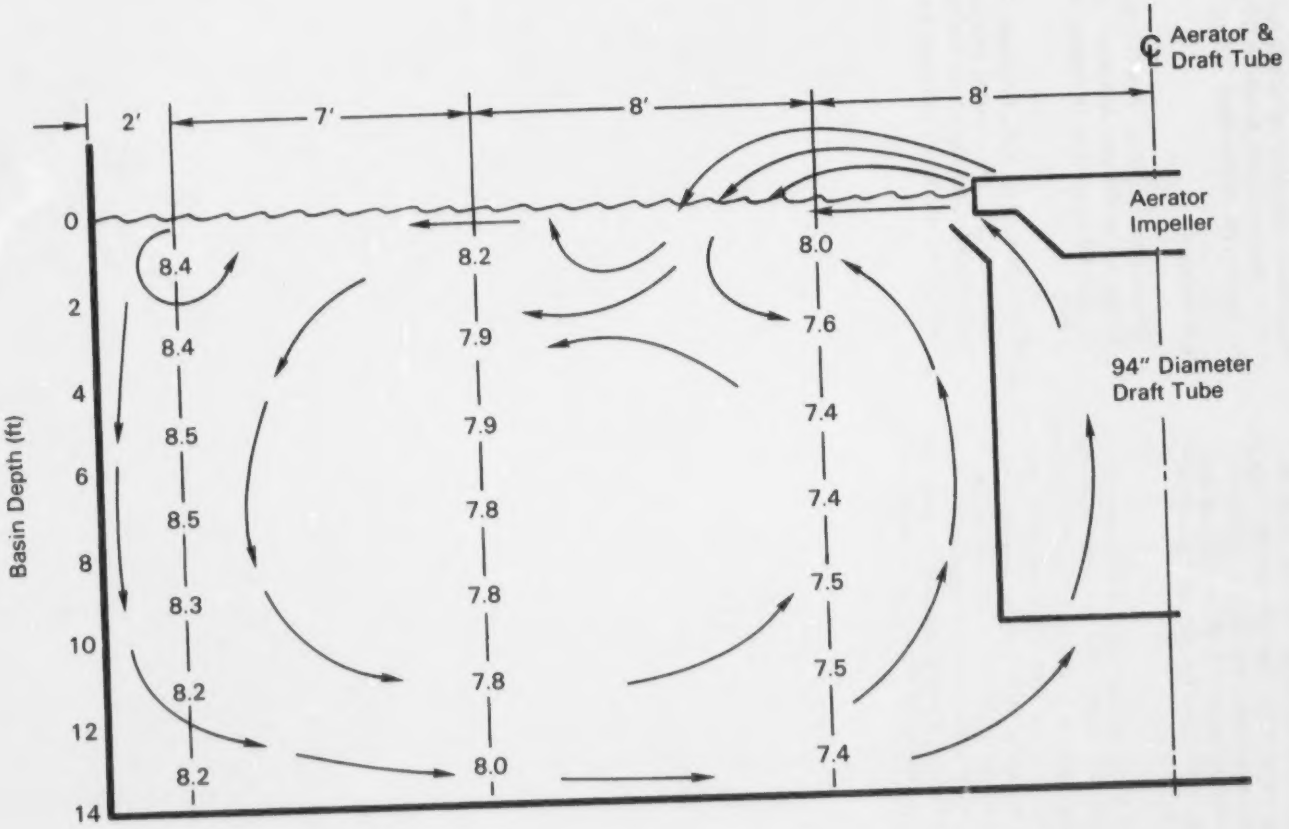
Figure 5. Afternoon Alpha and Beta Test Data Steady State Test at Lawrence, Kansas



a. Final Effluent — Direct Analysis

b. Tap Water — Direct Analysis

Figure 6. Dissolved Oxygen Profile in Aeration Tank in Afternoon Tests at Lawrence, Kansas


℄ Aerator & Draft Tube
2'
7'
8'
8'
Aerator Impeller
94" Diameter Draft Tube
Basin Depth (ft)
0
2
4
6
8
10
12
14
8.4
8.4
8.5
8.5
8.3
8.2
8.2
8.2
7.9
7.9
7.8
7.8
7.8
8.0
8.0
7.6
7.4
7.4
7.5
7.5
7.4

Conclusions

The testing at Lawrence, Kansas, and to a limited extent, the testing at Midland, Texas, demonstrated that it is possible to obtain comparable results by the steady state and the non-steady state methods. The use of endogenous respiration rather than the fed system offers an easier technique for evaluating aeration equipment by the steady state method. However, at oxygen uptake rates below 60 mg/l·hr, good results can be obtained with either method.

The steady state test has greater potential for error than the non-steady state test, since more analyses must be made and correlated together. Testing at the Kansas and Texas sites demonstrated the ease in which errors in measurement can be made, whether due to sampling techniques or instrument problems. From a practical point of view, the non-steady state test appears to be the best method for evaluating aeration equipment performance, especially when tested in the actual aeration tank with continuous data recording to permit evaluation of surge flows as well as oxygen transfer.

Acknowledgements

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References

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2. Stukenberg, J.R., V.N., Wahbeh, and R.E., McKinney, "Experiences in Evaluating and Specifying Aeration Equipment", Journal Water Pollution Control Federation, 49, p. 66, January 1977.

Problems Encountered in Steady State Field Testing of Aerators and Aeration Systems

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The two general methods currently in use for determining the oxygen transfer capacity of any aerator or aeration system are the non-steady state, clean water technique and the steady state test method conducted under process conditions, which, for an activated sludge aeration basin, means testing after a wastewater treatment plant is placed in operation and a suspension of microbial solids is developed. In the latter case, the liquid in which the aerator is tested has an oxygen uptake which must be considered in the test measurements and the oxygen transfer rate of the aeration system.

The general mathematical models used for both test methods are similar and are based on the same basic assumptions; the most important, and incidently the most frequently ignored, is that of a completely mixed liquid mass in which the oxygen uptake and transfer are occurring. Therefore, theoretically, both test techniques should give the same end results. However, from a practical standpoint this is not readily achieved and the basic reasons and problems will be discussed in this paper.

The fundamental mathematical model that has been adopted to describe the oxygen transfer phenomenon when oxygen is supplied to a liquid mass by some type of aeration device is:

$$dC/dt = K_La\,(C^*-C) - R \quad (1)$$

where:

dC/dt = rate of change of DO concentration

K_La = overall transfer coefficient under process conditions

C^* = DO saturation concentration under process conditions

R = respiration rate or oxygen uptake rate of biological organisms

In the non-steady state, clean water test, the value of $R = 0$, so that Equation 1 can be used directly, or in its integrated form, to determine the value of K_La by taking measurements of C for various values of t. The details of such testing and determination of the oxygen transfer rate from the value of K_La have been widely reported in the literature and will not be discussed in detail in this paper.

A basic assumption implied in the use of Equation 1 for both non-steady state, clean water testing and in tests made under process conditions (aeration in activated sludge basins) is that the oxygen transfer rate is liquid film limiting through the air (oxygen)/water interfaces. This is true as long as the reaction of the oxygen-using mechanism (biological organisms) is relatively slow compared to, say, chemical reactions. This may not be completely true if the value of R is very high, which could be the case for high concentrations of organisms and high turbulence levels. Such conditions would reflect a different value of K_La for process conditions than that obtained in clean water.

As will be discussed in more detail later, K_La is affected by liquid temperature (and also gas temperature, if different from that of the liquid) and by the liquid characteristics and composition. These effects must be quantitatively determined if values of oxygen transfer rates of aerators obtained in clean water and under process conditions are to be the same. Since different types of aerators and aeration systems must be compared for performance under "standard conditions," the identification and quantification of the factors that affect K_La under actual process conditions is generally the greatest obstacle to the use of steady state tests for evaluation of aerator performance under field conditions. Of course, such evaluation for a specific installation does not require conversion of the test results to clean water conditions necessarily.

Steady State Test Methods

There are several techniques that can be used for evaluating aerator performance under process conditions, that is, in activated sludge aeration basins. All of these techniques require that a so-called steady and stable state be established as far as organic load, mixed liquor suspended solids concentration, and oxygen uptake rate are conconcerned. This requires that any aeration basin be controlled to a degree that is not normally obtained in most wastewater treatment plants, especially those handling municipal wastewater. If aerator tests under process conditions are to be made, it is necessary to control the flow and organic load to any aeration basin so that the BOD load and the mixed liquor suspended solids concentration are substantially constant for at least a day and preferably longer preceeding a test. The load should be adjusted so that the steady state DO concentration is above 3 mg/l, but not over 5 mg/l (for air systems).

Testing under process conditions has been described by Eckenfelder (2), Nogaj and Hurwitz (9), and Kayser (4). A common method for obtaining the value of K_La is to determine R by taking a sample of the mixed liquor, raising its DO level by rapid aeration, and then measuring the drop in DO with respect to time by use of a fast-response DO meter. The value of K_La is then calculated from Equation 1, since for steady state conditions, $dC/dt = 0$. The relation is as follows:

$$K_La = R/(C^*-C^1) \qquad (2)$$

where:

C^1 = DO concentration at point where sample is taken for determining R

A problem regarding use of this technique is that for localized types of aerators (surface entrainment or submerged turbine type), the value of C^1 will vary from point to point spatially, thus necessitating use of an average value or several determinations of R from different sample points. Moreover, the externally determined value of R is not necessarily the value in the basin at the sample point, since it has been shown that R is dependent on the turbulence level. Also, the biological organisms in the basin are receiving food at some steady rate and this is not true of the organisms in the test jar. In fact, the value of R obtained in the external test jar can be increased by supplying some food during the oxygen uptake test.

Stukenberg *et al.* (10) discuss the matter of R varying throughout a basin. Of course, the value of K_La can be determined for several BOD loads, or different average values of R, and such values if plotted against the related value of C^1 can be used to calculate K_La from the slope of the resulting straight line, per Equation 2. This will provide a check on the proper value of C^* against the calculated or assumed value since it is the value of C^1 when $R = 0$, or the intercept on the horizontal axis of the straight line connecting different values of R and C^1.

For diffused air systems, a better and more direct method of determining the value of oxygen transfer rate, or K_La, can be used. This is based on making an oxygen balance around the aeration basin by measuring the oxygen concentration (or CO_2) in the off-gas. This method was used by Leary *et al.*(5) for determining air diffuser efficiency at the Milwaukee plant and also by Lister and Boon (6) in England. The latter compared the off-gas measurement method for determining K_La to measuring the uptake rate, R, externally. The latter method gave K_La values up to 30% higher, which they attributed to the much higher turbulence level in their respirometer than existed in the aeration basin.

Another method that has been used successfully to measure the oxygen transfer rate under process conditions involves establishing a steady and stable condition in the aeration basin, then shutting off the aeration and raw sewage and recycle flows, allowing the DO to drop to zero, and then starting up the aeration and flows and measuring the values of C and t at several points in the aeration basin until a stable value of C is obtained. This value should be at least equal to 3 mg/l but below 5 mg/l for air systems. The value of K_La is determined by plotting values of C vs t, drawing a smooth curve through the points, then graphically (or arithmetically) calculating dC/dt for different values of t. The latter should give a straight line, if the system is stable, whose slope is K_La. This comes about by transforming Equation 1 to the following form:

$$dC/dt = (K_La\,C^*-R) - K_La\,C \qquad (3)$$

In this procedure, K_La is determined without the need to measure R externally, or even to estimate the value of C^* under the process conditions. Some data using this procedure were recently obtained for an aerator installation where the owner required definite proof regarding the amount of oxygen that was being supplied by the aerators under existing process conditions. The data are shown in Figure 1 and 2, together with the calculated values of K_La and R. The latter can be calculated from the intercept of the straight line at $C = 0$, as is apparent from Equation 3, after determining the proper value for C^* under existing process conditions.

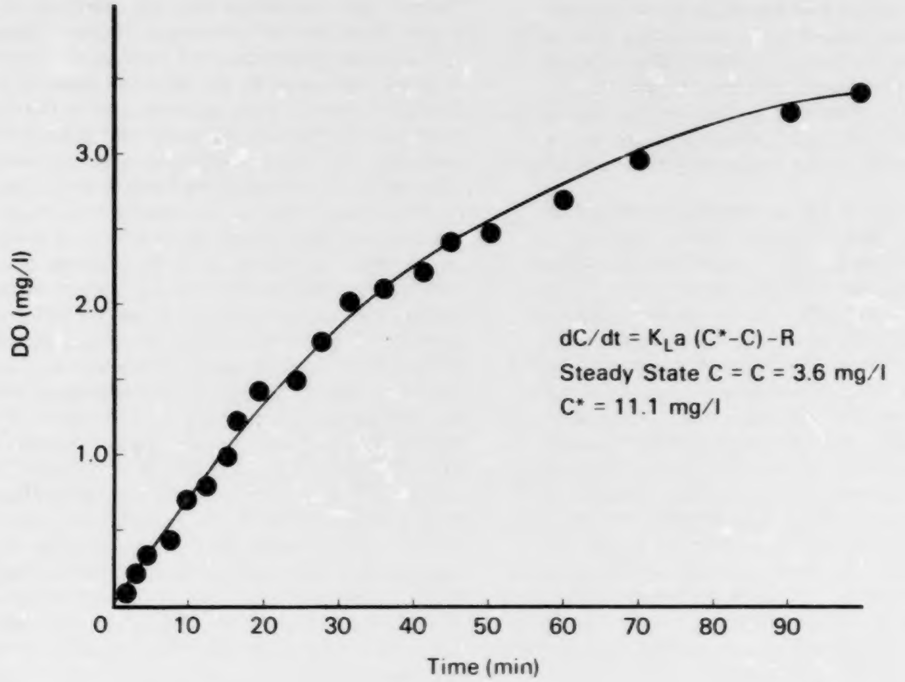
In all the above described test methods, in order to determine the true oxygen transfer rate of the aeration system, the amount of oxygen that is carried out by the flow of wastewater and recycle should be added to that calculated due to the uptake rate, R. Usually, this will be under 5% of the uptake rate for normally loaded and respiring activated sludge.

Conversion of Oxygen Transfer Rate Data to Standard Conditions

Though steady state testing under process conditions provides valuable information relating to the oxygen transfer characteristics and efficiency of aerators under actual specific operating conditions, such data are difficult to convert to standard conditions, which are defined as transfer into clean water at 20°C, zero DO, and sea level pressure. In order to compare and evaluate various designs of aerators and aeration systems, it is necessary that their performance be determined for standard conditions. It is then the responsibility of the design engineer to convert such standard conditions data to the performance that can be expected under actual specific operating conditions for the treatment of a particular wastewater over some temperature range.

To convert oxygen transfer data obtained under process conditions to clean water performance, the major inadequately known factor is the so called α factor, which is influenced primarily by surface-active agents present in wastewaters and produced as various soluble organics during biodegradation. Their physical effect on oxygen transfer from a gas through the liquid film has been described in several papers on aeration of wastewaters. Two papers by Mancy *et al.* (7) (8) give excellent basic physical explanations. A very significant item relating to

Figure 1. Reaeration of Activated Sludge Basin Under Process Conditions

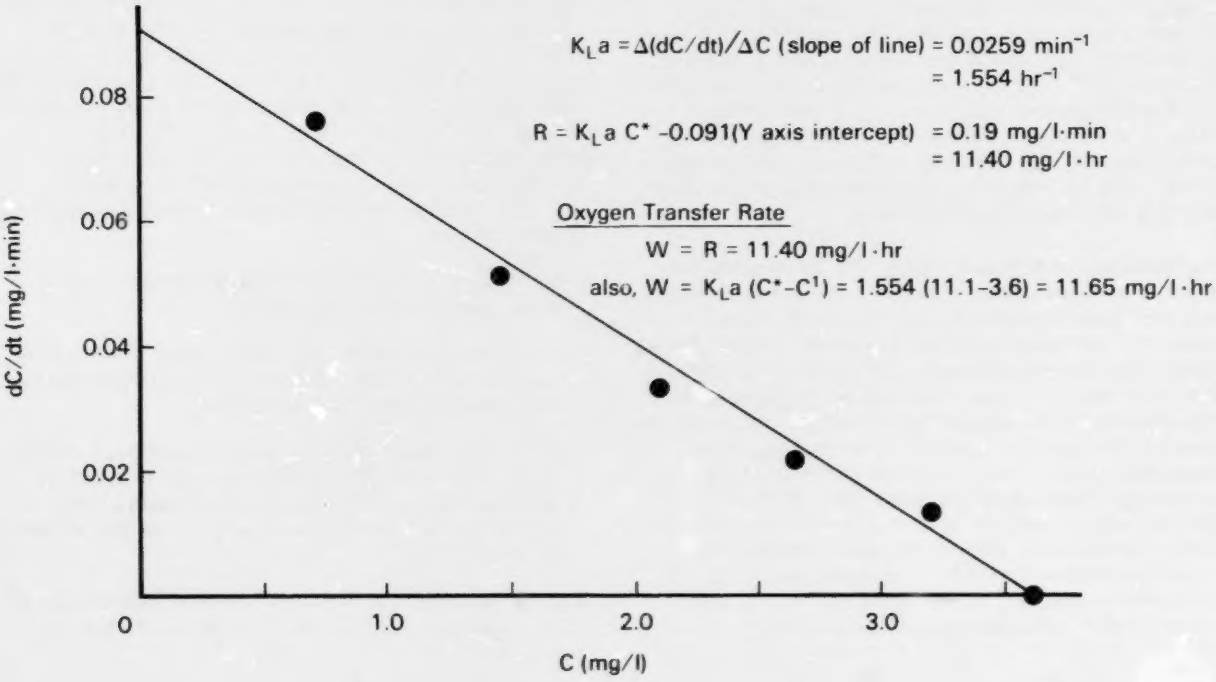


$dC/dt = K_La\ (C^* - C) - R$

Steady State C = C = 3.6 mg/l

$C^* = 11.1$ mg/l

Figure 2. dC/dt Values from Figure 1 Curve



$K_La = \Delta(dC/dt)/\Delta C$ (slope of line) = 0.0259 min^{-1} = 1.554 hr^{-1}

$R = K_La\ C^* - 0.091$(Y axis intercept) = 0.19 mg/l·min = 11.40 mg/l·hr

Oxygen Transfer Rate

W = R = 11.40 mg/l·hr

also, $W = K_La\ (C^* - C^1) = 1.554\ (11.1 - 3.6) = 11.65$ mg/l·hr the factor α is that for a given wastewater or concentration of surface-active agents, α can be vastly different for different types of aeration systems. Systems producing high levels of turbulence and interface renewal rates produce much higher values for α, everything else being similar, than do low-turbulence systems. For example, Downing and Boon (1) report that water with about 5 mg/l of detergents or similar surface-active agents can give an α value of 0.4 for fine bubble diffusers and a value of α up to 1.5 for surface type mechanical aerators.

Obviously, an engineer must be able to estimate the value of α to use for any particular type of aeration system in order that he be able to convert clean water performance data to that to be expected under particular process conditions. Data obtained for various aeration systems using previously described steady state techniques should be compared with clean water test data in order to have some basis for estimating values of α to use in sizing aerators. Sometimes it will be necessary to make non-steady state tests in clean water to which some detergent has been added to obtain an estimate of how surface active agents influence the performance of any particular aeration system. The English literature on aerator testing indicates that it is common practice to make the non-steady state tests in clean water to which 5 mg/l of anionic detergent have been added, since their tests under process conditions indicate that this roughly simulates the effect obtained in activated sludge aeration basins treating domestic wastewater.

The other important parameter that must be considered in converting test data for oxyen transfer rates obtained under process conditions, and also from clean water tests, is that of liquid temperature. In addition, if the water temperature is considerably different from the air temperature from which the oxygen is being obtained, the value of C^* to use should correspond to a temperature that is between that of the water and of the air. There is inadequate information in regard to this to permit any accurate calculation of the true temperature of the liquid film when the air temperature is considerably different from that of the water.

The influence of temperature on K_La resultes from the effect of temperature on molecular diffusivity of oxygen in a thin liquid film of a certain thickness. However, the mathematical model on which Equation 1 is based does not incorporate any parameter that would quantify this film thickness. The model is based on the assumption that diffusion of the oxygen outside of the interfacial film is by turbulent diffusion which is several orders of magnitude greater than molecular diffusion and is not temperature dependent. Temperature affects the liquid viscosity and it has been shown that the concentration of suspended solids in activated sludge increases the effective viscosity. This has been demonstrated by the great differences in liquid pumping action and flow pattern which some types of localized aerators exhibit in clean water and in mixed liquor having several thousand mg/l of suspended biological solids.

There is good evidence that the effect on K_La of liquid temperature and of the concentration of suspended solids will depend greatly on the intensity and type of turbulence generated by the aeration system. Fine-grained microturbulence, such as produced by the high shearing action of mechanical aerators, will greatly reduce and minimize the effect of changes in liquid viscosity due to temperature and suspended solids and, thus, the effect on molecular diffusion. In other words, high energy systems will not exhibit large effects of temperature on K_La. Imhoff and Albrecht (3) very clearly show how liquid temperature affects the relative oxygen absorption (for comparable oxygen deficit values) for different aeration systems. They indicate a significant correction for temperature for low-turbulence systems, such as bubble diffusers and certain types of mechanical aerators, while the correction is practically non-existent for high-turbulence systems. They classify systems having energy inputs in excess of 0.75 hp/$1{,}000^3$ as being high-turbulence systems. This is very likely an over-simplified designation. In any case, it is fairly obvious that a constant temperature correction factor for K_La for all types of aerators is not justified. Moreover, low-turbulence systems will exhibit greater effects on K_La of high concentrations of mixed liquor suspended solids than will high-energy systems, thus further complicating conversion of test data obtained under process conditions to standard conditions.

Summary

To summarize, testing aerators under process conditions should be done whenever possible in order to obtain information that will provide a broader basis for converting such test data to performance under standard, clean water conditions. This then permits the design engineer to have more reliable information for converting the performance information given for aerators under standard conditions to the wide variety of practical operating conditions in wastewater activated sludge aeration basins.

The problems encountered in testing aerators under process conditions are as follows:

1. Difficulties in establishing steady state, stable conditions as far as BOD loading and mixed liquor solids are concerned in treatment plants.

2. Sampling problems when using the off-gas analysis method for evaluating performance of diffused air systems. Replicate testing reports predict a 95% confidence level for a mean oxygen transfer efficiency of about 20%.

3. Data for externally measured oxygen uptake rates for samples from aeration basin mixed liquor are not

necessarily representative of the actual average uptake rate in the basin. A more preferable method is to use a reaeration technique which permits determining K_La under process conditions without separate measurement of R.

4. Conversion of steady state process test data to oxygen transfer rates under standard conditions requires knowledge of the *in situ* value of α, which is dependent on the chemical composition of the liquid phase, turbulence-level, and concentration of suspended solids in the aeration basin.

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Section V. Tracer Methods

Use of Tracers for Evaluation of Oxygen Transfer

23

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As energy costs continue to escalate, we are faced with two difficult challenges related to the design and operation of oxygen transfer equipment:

1. Oxygen transfer equipment manufacturers must continue to develop more energy-efficient designs.
2. Engineers designing wastewater treatment facilities must specify oxygen transfer requirements that are adequate to get the job done with a minimum of wasteful overdesign.

The economic aspect of these challenges is great. Ten years ago, a 50-hp mechanical aerator sold for about $6,500 and required some $6,200/yr for electrical energy. Today, the same basic aerator sells for about $16,000 and associated electrical energy costs may exceed $12,000/yr.

Over the design life of an oxygen transfer system, even modest equipment efficiency improvements can represent considerable potential cost savings. However, overly conservative facility designs that result in too much installed oxygen transfer capacity can burden owners with unnecessary energy costs that might offset savings provided by improved equipment efficiency.

If these two challenges are to be met, we must have accurate and reliable testing methods available for evaluating oxygen transfer. We need testing methods that can be reliably used to evaluate individual oxygen transfer devices under carefully controlled test conditions, but more importantly, we need testing methods that can be used to accurately evaluate oxygen transfer in total systems operating in the field under real process conditions.

Most equipment testing done today depends on either a direct or indirect DO balance that is difficult to conduct because oxygen is a common, sparingly-soluble gas that is both chemically and biologically reactive over a wide range of environmental conditions. Oxygen is susceptible to so many influencing factors that accurate measurement of simple physical transfer in an operating treatment plant is difficult if not impossible. Conventional clean water tests are limited to complete mix conditions, and yet complete mix is not achieved or required in many (most) field installations.

To overcome the problems associated with present conventional testing methods, the gaseous tracer method is proposed. This paper describes the tracer method for making direct measurement of gas transfer performance in test tanks or in operating field installations under real process conditions. Previously reported applications of the tracer method are discussed and potential applications are also identified. Much of the material in this paper is taken from an earlier work by Neal and Tsivoglou (6).

Tracer Method History

The tracer method involves the simultaneous use of an inert radioactive gaseous tracer for oxygen (or any other gas of interest) and a radioactive dispersion/dilution tracer.

The tracer method for measurement of gas transfer capacity is not new in that it was originally developed about 12 yr ago for stream reaeration measurement by Tsivoglou and others (7)(8)(9) at the U.S. Public Health Service (USPHS) in Cincinnati OH.

Since the first field application in 1966, the tracer method has been used to provide hundreds of accurate stream reaeration measurements in several states as summarized by Tsivoglou and Neal (8). The tracer method is now widely considered to be the only reliable method for accurate measurement of stream reaeration capacity.

From 1968 to 1970, Gordon and Etzel (2) conducted mechanical aerator performance tests in a pilot-scale test tank using the tracer method (gas tracer only). Although the complete studies have not been published, Gordon (3) did provide most of the test results in a 1976 "Communication" to the Water Pollution Control Federation Journal. (The Gordon and Etzel studies will be discussed further in a subsequent section of this paper.)

In 1971, Neal (5) applied the tracer method to four different aeration systems at operating wastewater treatment facilities as part of a graduate study program at Georgia Tech. In 1972, Neal and Tsivoglou (6) presented the studies at the 45th Water Pollution Control Federation Conference and the paper was subsequently published in 1974.

The Army Corps of Engineers is now sponsoring tracer measurements of gas transfer capacity in hydraulic models at the Waterways Experiment Station and at Georgia Tech. The purpose of these studies is to improve techniques for predicting gas transfer characteristics at hydraulic structures, which can thereby lead to improved designs.

Oxygen Transfer Considerations

In order to provide a common basis for describing the tracer method, the following basic oxygen transfer considerations are presented.

A commonly used general mathematical expression describing aeration of wastewater is:

$$d(\beta C^* - C)/dt = -\alpha K_L a(\beta C^* - C) + R \qquad (1)$$

where:

C^* = DO saturation concentration for clean water, mg/l

C = DO concentration in water at time t, mg/l

K_La = gas transfer rate coefficient for oxygen in clean water, hr^{-1}

β = ratio of the DO saturation concentration for wastewater to that for clean water

α = ratio of the oxygen transfer rate coefficent for wastewater to that for clean water

R = the rate of DO utilization by the system, mg/l·hr

The product (αK_La) represents the actual oxygen transfer rate coefficient for the wastewater.

DO Saturation. The DO saturation limit in clean water, C^*, may be obtained from "Standard Methods" knowing the water temperature, the prevailing atmospheric pressure, and the pressure of saturated water vapor at the specified water temperature. In diffused aeration systems, where air is released at some depth below the free water surface, the solubility of oxygen is influenced by the increased partial pressure of oxygen in the bubbles that have been compressed by the weight of the water above and by the decreasing partial pressure as oxygen is absorbed from the bubbles.

Gas Transfer Rate. For any specific aeration system, the oxygen transfer rate coefficent for clean water, K_La, is dependent on turbulent mixing, aeration equipment characteristics, geometric configuration of the system, and operating conditions within the system.

The effect of temperature on K_La may be described by:

$$(K_La)_{20} = (K_La)_T/\theta^{(T-20)}$$

in which $(K_La)_{20}$ is the gas transfer rate coefficient for oxygen at 20°C, $(K_La)_T$ is the gas transfer rate coefficient for oxygen at temperature T, and θ is the temperature correction coefficient. Tsivoglou (4) has demonstrated the mean value of θ, from theoretical considerations, to be:

$$\theta_{mean} = 1.022 \pm 0.004$$

in the temperature range 0 to 30°C. This predicted value agrees well with the detailed observations of Churchill et al. (1), which provided a value of 1.024. It should be noted that these values of θ were determined by considering clean water conditions and should be applied to wastewater conditions with caution.

DO Saturation in Wastewater. Pollutants can alter the DO saturation limit. "Standard Methods" shows the depression of oxygen solubility caused by chloride ion concentration. Generally, however, the ratio of C^* for wastewater to C^* for clean water, β, is not readily predicted and must be determined experimentally.

Gas Transfer in Wastewater. Various pollutants alter the ability of water to transfer gas molecules. This alteration causes the value of K_La to vary under identical mixing conditions, depending on whether clean water or wastewater is being aerated. The ratio of K_La for wastewater to K_La for clean water, α, is related not only to the waste constituents and concentrations, but also to the turbulent mixing conditions within the fluid. This means that determination of α must be conducted either in the full-scale system or in a system that duplicates the turbulent mixing within the real system.

DO Utilization. Oxygen utilization is the result of a biochemical process and as such is independent of the physical gas transfer process which is the subject of this paper. The fact that both of these independent processes influence the same parameter, DO concentration, is a point of confusion for some investigators. It is emphasized that the actual gas transfer coefficient for oxygen in a real wastewater system is equal to the product of α and K_La. Similarly, the actual DO saturation concentration is equal to the product of β and C^*. In order to specify actual field aeration performance, the actual oxygen transfer rate coefficient, αK_La, and DO saturation concentration, βC^*, must be known.

Tracer Measurement of Oxygen Transfer

The tracer method depends on the simultaneous use of a gaseous tracer for oxygen and a dispersion/dilution tracer. In selecting a tracer gas, there are several criteria to be considered:

1. It must be possible to relate the physical transfer properties of the tracer gas to those of oxygen over the expected range of environmental conditions.
2. The tracer gas must not be susceptable to biological or chemical reactions that might be encountered in an operating aeration basin.

3. The tracer gas must be reasonably easy to handle and measure.
4. The tracer gas must not be too expensive.
5. The tracer gas must be safe to use.

After much careful consideration and numerous laboratory studies, radioactive krypton-85 was selected by Tsivoglou et al. (7) as the overall best gaseous tracer for oxygen. Krypton is a monatomic "noble" gas that is neither chemically or biologically reactive. As a radioactive isotope, krypton-85 can be accurately measured at very low concentrations by liquid scintillation methods. Krypton-85 is readily available and inexpensive. Because krypton-85 is a radioactive material, a state or federal license is required to use it, but with proper handling, krypton-85 can be safely used as a gas tracer.

In order to use krypton-85 gas as a tracer for the evaluation of oxygen transfer, it is necessary to know the relative transfer capabilities of the two gases. To experimentally evaluate relative gas exchange rates, Tsivoglou et al. (7) (8) conducted a series of laboratory tests in which simultaneous transfer of gas pairs (e.g., oxygen and krypton) could be observed in the same turbulent water reactor. These experiments were conducted to determine the relative gas exchange capabilities of krypton-85, oxygen, radon-222, hydrogen, helium, nitrogen, and carbon dioxide.

Krypton-85 transfer in water can be described by a modification of the basic oxygen transfer expression, Equation 1. The concentration of krypton-85 present in the earth's atmosphere can be considered zero for practical purposes. Therefore, any dissolved krypton-85 in a turbulent water system will be steadily lost from the water to the atmosphere according to the expression:

(3) $$dC_{kr,t}/dt = (K_{kr})_{obs}\, C_{kr,t}$$

in which $C_{kr,t}$ is the concentration of dissolved krypton-85 in the water at time t and $(K_{kr})_{obs}$ is the observed gas transfer rate coefficient for krypton-85.

In the USPHS laboratory experiments, it was found that the relative transfer capabilities of gas pairs, measured as the ratio of gas transfer rate coefficients, is constant. From 36 simultaneous krypton-85 and oxygen reactor experiments, it was found that the ratio of gas transfer rate coefficients, $K_{kr}/K_L a$, is 0.83 with a standard deviation of 0.04. This relationship is illustrated in Figure 1.

A logical explanation of the constant ratio of gas transfer rate coefficients from gas pairs was derived by Tsivoglou (9) from consideration of Einstein's basic law of diffusion. The derived expression relates gas exchange ratios to gas molecule diameters as:

(4) $$K_1/K_2 = D_{m,1}/D_{m,2} = d_2/d_1 = \text{constant}$$

where the subscripts 1 and 2 identify the two gases considered, K is the gas transfer rate coefficient, D_m is the molecular diffusion coefficient, and d is the effective diameter of the gas molecule. Unfortunately, there are very little reliable diffusion and molecular diameter data suitable for testing of Equation 4. However, limited experimental verification of the relationship has been documented for oxygen, nitrogen, and carbon dioxide.

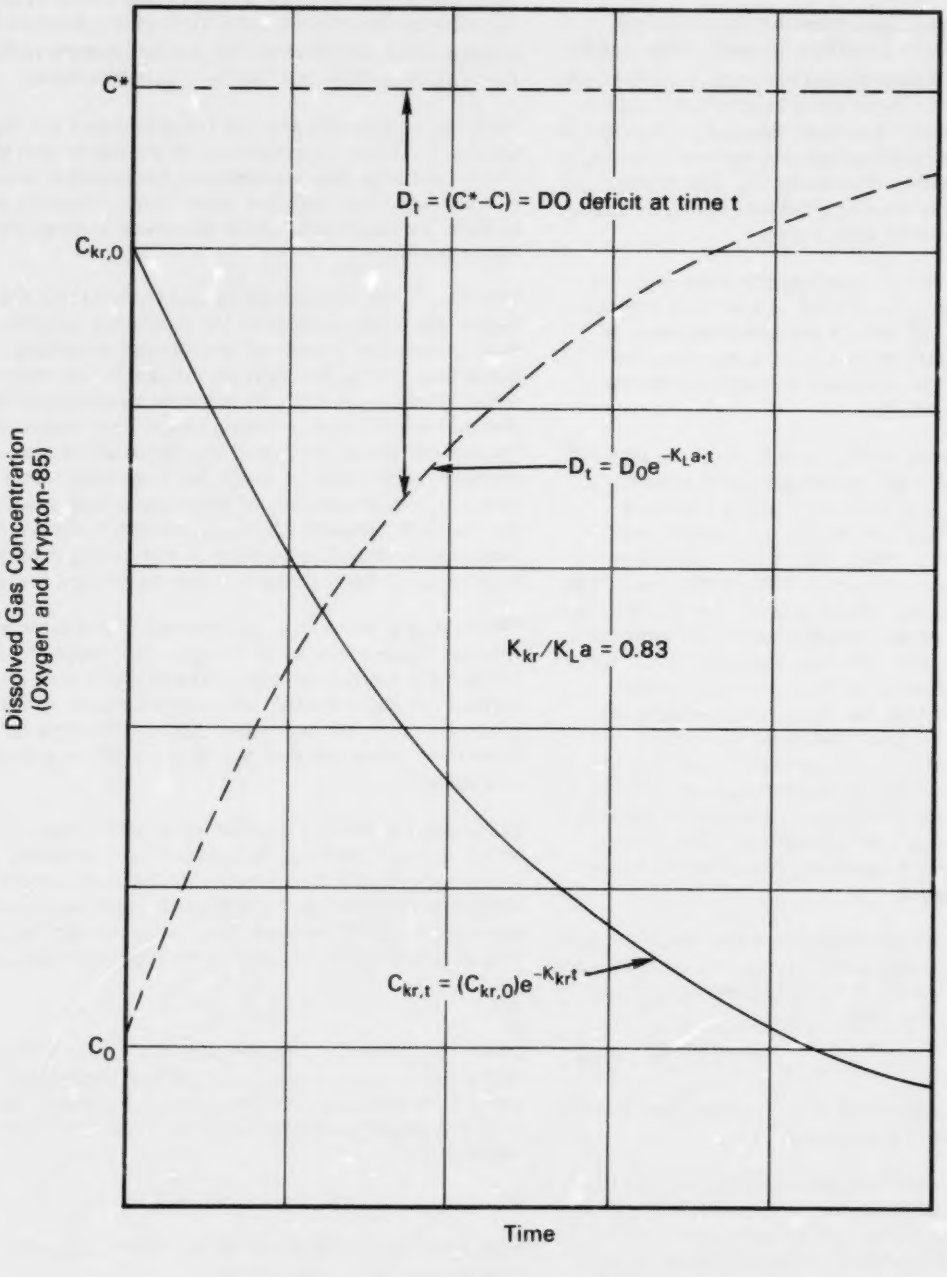
From the 36 laboratory experiments, it was found that the $K_{kr}/K_L a$ value of 0.83 is not significantly affected or changed by temperature in the range of 10 to 30°C, by the degree of turbulent mixing, by the direction in which the gases are transferring, or by the presence or absence of a broken water surface. The presence of the dissolved solids in a natural ground water and the presence of alkylbenzene sulfonate (ABS) in two experiments had no significant effect on the ratio.

Effect of Pollutants. Because the ratio of K values for a pair of gases is considered to be a fundamental molecular property of the two gases, there is no reason to expect that pollutants in the water would significantly affect the ratio of K values. Various pollutants alter the ability of gas molecules (oxygen, krypton, or others) to enter and escape from water. This alteration causes the value of the individual K values to vary under apparently identical hydraulic and environmental conditions, depending on whether clean water or polluted water is being considered. The pollutant effect on gas transfer is not only related to the pollutant constituents and concentrations, but also to the turbulent mixing regime within the fluid.

The tracer method has been used to measure pollutant effects on gas transfer in a series of laboratory investigations, as described by Neal (4) and Tsivoglou and Wallace (10). In these studies, tests were conducted to establish "alpha" factors for LAS, NTA, and mineral oil in distilled water and for grossly polluted stream waters. The fundamental reasons for pollutants influencing gas transfer have not been well established, but it is speculated that the effect of pollutants on physical gas transfer is principally hydrodynamic in nature and molecular scale hydrodynamics are probably involved.

Other Experiments. The previously mentioned work of Gordon (3) and Etzel (2) is considered here because it provides an independent set of pilot-scale data on simultaneous oxygen and krypton-85 gas transfer. For the first series of pilot-scale studies, a 15-ft diameter pool with a plastic liner was filled with 2000 gal of well water and a 2-hp floating mechanical aerator was centered in the pool. Five tests were conducted in the pool with simultaneous oxygen transfer measurement (by the non-steady state reaeration method using cobalt and sodium sulfite) and krypton-85 transfer measurement with duplicate sampling points designated "left" and "right" for each test. Table 1 summarizes the five well water tests with reported $K_{kr}/K_L a$ values ranging from 0.71 to 0.89 and a mean for all ten values of 0.82 with a

Figure 1. Relative Transfer Properties of Oxygen and Krypton-85 in Water



standard deviation of 0.06 (7% of the mean). Although the scatter of these results is greater than that associated with the experimental work at the USPHS, there is reasonably good agreement with respect to the K_{kr}/K_La values reported (0.83 + 0.04 USPHS and 0.82 + 0.06 Gordon and Etzel).

After the five well water experiments, the pool was drained and refilled with a mixture of well water, cobalt, instant milk, and activated sludge to create a biologically-active condition. Eleven tests were conducted in this mixture with simultaneous oxygen transfer evaluation (by the non-steady state respiring system method using sodium sulfite to disturb the equilibrium DO) and krypton-85 transfer measurement with duplicate sampling points as used for the previous well water tests.

Table 2 summarizes the 11 wastewater tests with K_{kr}/K_La values ranging from 0.62 to 2.07 and a mean for all 22 values of 0.96 with a standard deviation of 0.29 (30% of the mean). With such a large range of observed ratios there is obviously a problem with the reliability of the test results.

Examination of the reported K_{kr} and K_La values shows that while the krypton test results are quite consistant with a mean $\alpha(K_{kr})_{20}$ of 0.27/min and a standard deviation of about 4% of the mean, the oxygen results are quite erratic with a mean $\alpha(K_La)_{20}$ of 0.30/min and a standard deviation of about 20% of the mean. The range of the 22 $\alpha(K_{kr})_{20}$ values is 0.04/min (0.29-0.25), which is about 15% of the mean value (0.27/min); the range of the 22 reported $\alpha(K_La)_{20}$ values is 0.28/min (0.42-0.14), which is about 93% of the mean value (0.30/min). It is clear that the oxygen test results in wastewater are erratic. The reason for this problem cannot be determined from the available data, but it is suspected that the non-steady state respiring-system method used for establishing the K_La values is inherently subject to such problems. In comparison, the gaseous tracer method provided consistent, reproducible results for the entire test series.

Assuming that mixing conditions were the same for both test series (well water and wastewater), an average alpha value can be computed for the wastewater mixture by assuming $\alpha = 1.0$ for the well water:

$$\alpha = K_{kr,\ \text{activated sludge}}/K_{kr,\ \text{well water}} = 0.27/0.29 = 0.93$$

This means that the net effect of the wastewater mixture on gas transfer was a 7% reduction.

Accounting for Dispersion and Dilution. In order to make the tracer method applicable to a full range of field situations, it is necessary to add a dispersion/dilution tracer to the gas. The dispersion/dilution tracer must accurately trace the movement of water without being subject to losses of any significance during the course of a gas transfer study. Tritium, in the form of tritiated water, has been selected as the dispersion/dilution tracer for use with the gaseous tracer, krypton-85. For all practical purposes, tritiated water is no more subject to adsorption or other losses than ordinary water and, because it is water, it is the ideal dispersion/dilution tracer for use in gas transfer studies. Tritium is also radioactive and can, therefore, be measured with good accuracy at low concentrations by the same liquid scintillation techniques used to measure krypton-85. Triated water and krypton-85 are low hazard radiotracers that can be safely used within regulatory limits.

Both the krypton-85 and the tritiated water are homogeneously mixed together in a single tracer-unit bottle. The krypton-85 gas is completely dissolved in water, and the bottle is filled with the water/tracer mixture so as to exclude air space into which dissolved krypton-85 might transfer.

The tracer unit is released by quickly breaking the bottle below the water surface in the operating aeration basin. Both tracers are dispersed and diluted according to the prevailing mixing and flow conditions in the system. The tritiated water provides an accurate measure of the total dispersion and dilution taking place. The dissolved krypton-85 gas is subject to the same dispersion and dilution as the tritiated water because both tracers are released simultaneously in a homogeneous mixture. Also, the dissolved krypton-85 gas is lost from the water because of physical gas transfer but, being a noble gas, krypton-85 is not subject to other significant losses.

The rate of change in concentration of the dissolved krypton-85 gas relative to the rate of change in concentration of tritium accurately reflects the rate of dissolved krypton-85 gas transfer. This rate of krypton-85 transfer can then be converted to the gas transfer rate for oxygen from the known value of $K_{kr}/K_La = 0.83$ as previously discussed.

Experimental Basis. Consider an aeration basin into which a single homogeneous solution containing the two tracers previously discussed is released at a point. By sampling at a fixed point within the basin as a function of time, the concentration ratio of krypton-85 to tritium can be established for each sampling time such that:

$$R_t = C_{kr,t}/C_{H,t} \tag{5}$$

where $C_{kr,t}$ and $C_{H,t}$ are the concentrations of krypton-85 and tritium, respectively, in a single sample taken at time t and R_t is the concentration ratio of the tracers at time t. Applying this ratio concept, Equation 3 may be modified to:

$$dR_t/dt = -{}^*(K_{kr})_{obs}\, R_t \tag{6}$$

with terms as defined previously. Solving Equation 6 and rearranging terms, we obtain:

$$^*(K_{kr})_{obs} = \ln(R_2/R_1)/-(t_2-t_1) \tag{7}$$

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* *αK_{kr} is used interchangeably with $(K_{kr})_{obs}$.*

Table 1. Summary of Pilot-Scale Well Water Test Results Adjusted to 20°C ($\theta = 1.024$) — Data from Gordon (3)

	Left Station			Right Station		
Run	$\alpha K_L a$# (min^{-1})	αK_{kr}‡ (min^{-1})	$\alpha K_{kr}/\alpha K_L a$†	$\alpha K_L a$# (min^{-1})	αK_{kr}‡ (min^{-1})	$\alpha K_{kr}/\alpha K_L a$†
1	0.39	0.29	0.74	0.38	0.29	0.82
2	0.41	0.21	0.71	0.38	0.32	0.84
3	0.35	0.28	0.80	0.36	0.32	0.89
4	0.35	0.31	0.89	0.37	0.31	0.84
5	0.36	0.30	0.83	0.37	0.30	0.81

For all 10 results:

#Mean $\alpha K_L a$ = 0.37
Std. Dev. = 0.02 (5% of mean)
Range = 0.06 (16% of mean)
Median Value = 0.37

‡Mean αK_{kr} = 0.29
Std. Dev. = 0.03 (10% of mean)
Range = 0.11 (38% of mean)
Median Value = 0.30

†Mean ($\alpha K_{kr}/\alpha K_L a$) = 0.82
Std. Dev. = 0.06 (7% of mean)
Range = 0.18 (22% of mean)
Median Value = 0.83

Table 2. Summary of Pilot-Scale Wastewater Test Results Adjusted to 20°C ($\theta = 1.024$) — Data from Gordon (3)

	Left Station			Right Station		
Run	$\alpha K_L a$# (min^{-1})	αK_{kr}‡ (min^{-1})	$\alpha K_{kr}/\alpha K_L a$†	$\alpha K_L a$# (min^{-1})	αK_{kr}‡ (min^{-1})	$\alpha K_{kr}/\alpha K_L a$†
1	0.23	0.27	1.17	0.28	0.27	0.96
2	0.42	0.26	0.62	0.24	0.26	1.08
3	0.26	0.26	1.0	0.35	0.29	0.83
4	0.28	0.28	1.0	0.32	0.28	0.88
5	0.28	0.27	0.96	0.32	0.27	0.84
6	0.41	0.28	0.68	0.32	0.28	0.88
7	0.25	0.29	1.16	0.33	0.27	0.32
8	0.32	0.25	0.78	0.30	0.27	0.90
9	0.32	0.27	0.84	0.33	0.28	0.85
10	0.35	0.26	0.74	0.25	0.26	1.04
11	0.14	0.29	2.07	0.29	0.29	1.00

For all 22 results:

#Mean $\alpha K_L a$ = 0.30
Std. Dev. = 0.06 (20% of mean)
Range = 0.28 (93% of mean)
Median Value = 0.325

‡Mean αK_{kr} = 0.27
Std. Dev. = 0.01 (4% of mean)
Range = 0.04 (15% of mean)
Median Value = 0.27

†Mean ($\alpha K_{kr}/\alpha K_L a$) = 0.96
Std. Dev. = 0.29 (30% of mean)
Range = 1.45 (151% of mean)
Median Value = 0.89

in which R_1 and R_2 represent the krypton-85 to tritium concentration ratios at times t_1 and t_2, respectively. Thus, the use of tritium provides an automatic correction for the effects of dispersion and dilution. The tracers yield the necessary data to determine the actual krypton-85 transfer coefficient, αK_{kr}, for the system and the conversion to an actual oxygen transfer coefficient, $\alpha K_L a$, is from the established gas exchange ratio, $K_{kr}/K_L a = 0.83$.

It is emphasized that the tracer method does not rely on any theoretical assumptions that cannot be verified. The tracer method is a direct measurement of gas transfer under real conditions of mixing, water quality (biological and chemical), flow, temperature, weather, etc.

Field Studies

In order to demonstrate the tracer method fully, full-scale aeration tests were conducted in four operating waste treatment oxygen transfer basins, two mechanical aeration systems and two diffused air systems.

Kenwood Plant. The Kenwood wastewater treatment plant is a 70,000-gpd extended aeration unit. The aeration basin has a volume of 70,000 gal with one 7.5-hp mechanical surface aerator. The plant treats domestic waste only. As the aeration tank is designed for nearly complete mix, two sampling stations were sufficient. Figure 2 shows the location of each sampling station and the dose release point used for each of the three studies conducted in the basin.

South Cobb Plant. The South Cobb wastewater treatment plant has one mechanically aerated activated sludge basin with a design treatment capacity of 2 mgd. The aeration tank has a volume of 502,000 gal with four 10-hp mechanical surface aerators. The South Cobb plant treats a combination of domestic and industrial wastes. Figure 3 shows the location of each sampling station. The dose location was the same for each of the three studies conducted in this basin.

South River Plant. The South River wastewater treatment plant has four parallel activated sludge basins equipped with diffused air aeration. Each basin has a volume of 476,000 gal and a design treatment capacity of 3 mgd. The air blowers provide approximately 2.1 mil ft^3 air/day to each of the four units. Two of the basins use Walker Process diffusers while the other two basins use Water Pollution Control Corporation diffusers. The first test at this plant was conducted in a Walker Process basin and the second test was conducted in one of the Water Pollution Control Corporation units. Figure 4 shows the sampling and dosing locations for both studies.

Field Results

From the eight separate field tracer studies, 23 independent observations of gas transfer capacity were obtained. Figures 5 through 7 are plots of the observed krypton to tritium concentration ratios versus time from typical tracer studies. Table 3 is a summary of all field test results.

In addition to providing accurate measurement of gas transfer, the tracer method also provides an excellent measure of dispersion and dilution. Figure 8 shows tritium versus time curves for four sampling stations used in Test No. 6 at the South Cobb plant, and Figure 9 shows the corresponding krypton to tritium ratio plots with the ratio scale shifted to show all four stations on one plot. If a more refined definition of the dispersion/dilution characteristics is needed, then more frequent sampling would be required in the first few minutes after the tracer release. It should be noted that while the South Cobb plant is far from a complete mix system, as demonstrated by the tritium curves in Figure 8, the gas transfer rates are quite readily determined as illustrated in Figure 9 because the tritium accurately and automatically accounts for dispersion and dilution.

The average krypton transfer rate coefficient, $(K_{kr})_{obs}$, obtained from the six observations at the Kenwood plant was 2.4 hr^{-1} with a range of 2.21 hr^{-1} to 2.5 hr^{-1}. The average $\alpha(K_L a)_{20}$ was found to be 2.6 hr^{-1} for the three studies.

The Kenwood plant experienced a sludge recycle pump failure causing mixed liquor quality to vary for each study. For the three Kenwood studies, the gas transfer raes increased, while the basin solids (settleable) decreased. It is noted that there appears to be a strong linear relationship between $\alpha(K_L a)_{20}$ and mixed liquor settleable solids for the three test results.

Test	$\alpha(K_L a)_{20}$ (hr^{-1})	Solids (ml/l)
1	2.43	100
2	2.64	50
3	2.81	12

Of course, it is not possible to draw any firm conclusions from only three data points but if a linear relationship is assumed, for every 1% increase in settleable solids there was a 1.5% decrease in the oxygen transfer rate coefficient.

For each Kenwood test, the two sampling stations, A and B, gave essentially idential $\alpha K_L a$ values, which indicate relative uniformity of gas transfer in the basin.

Three tests were conducted in the South Cobb plant and a total of 11 $\alpha(K_L a)_{20}$ values were obtained with a range from 3.32 hr^{-1} to 2.65 hr^{-1}. The South Cobb plant was experiencing a solids build-up problem during the test period and the plant operator reported mixed liquor settleable solids ranging from 400 ml/l to 600 ml/l. For the South Cobb test of August 4, 1971 (Test No. 6), settleable solids were measured at each of the four tracer sampling stations and it was found that settleable solids and tracer-measured $\alpha(K_L a)_{20}$ values were related.

Figure 2. Kenwood Plant Sampling Locations

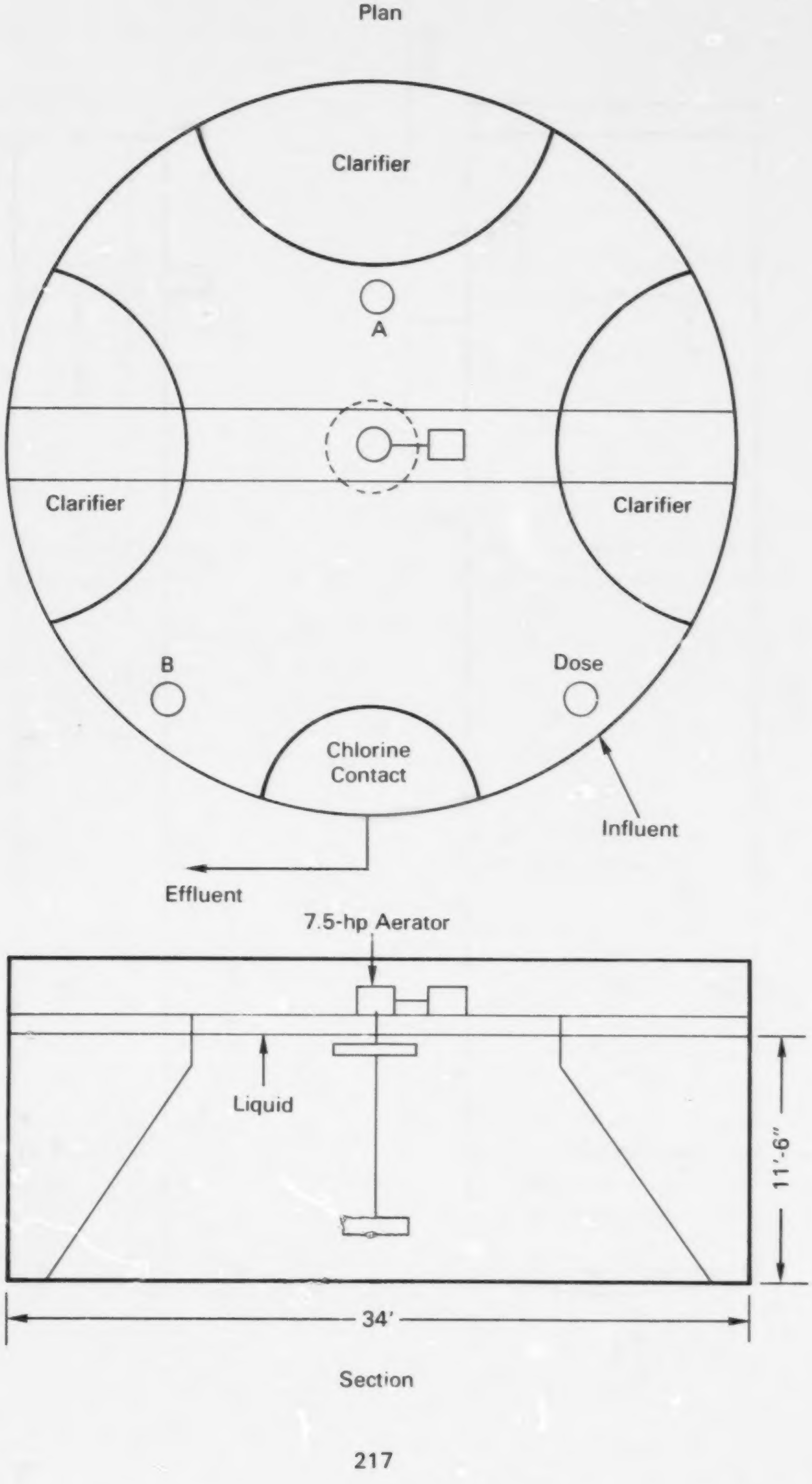
Plan
Clarifier
A
Clarifier
Clarifier
B
Dose
Chlorine Contact
Influent
Effluent
7.5-hp Aerator
Liquid
11'-6"
34'
Section

Figure 3. South Cobb Plant Sampling Locations

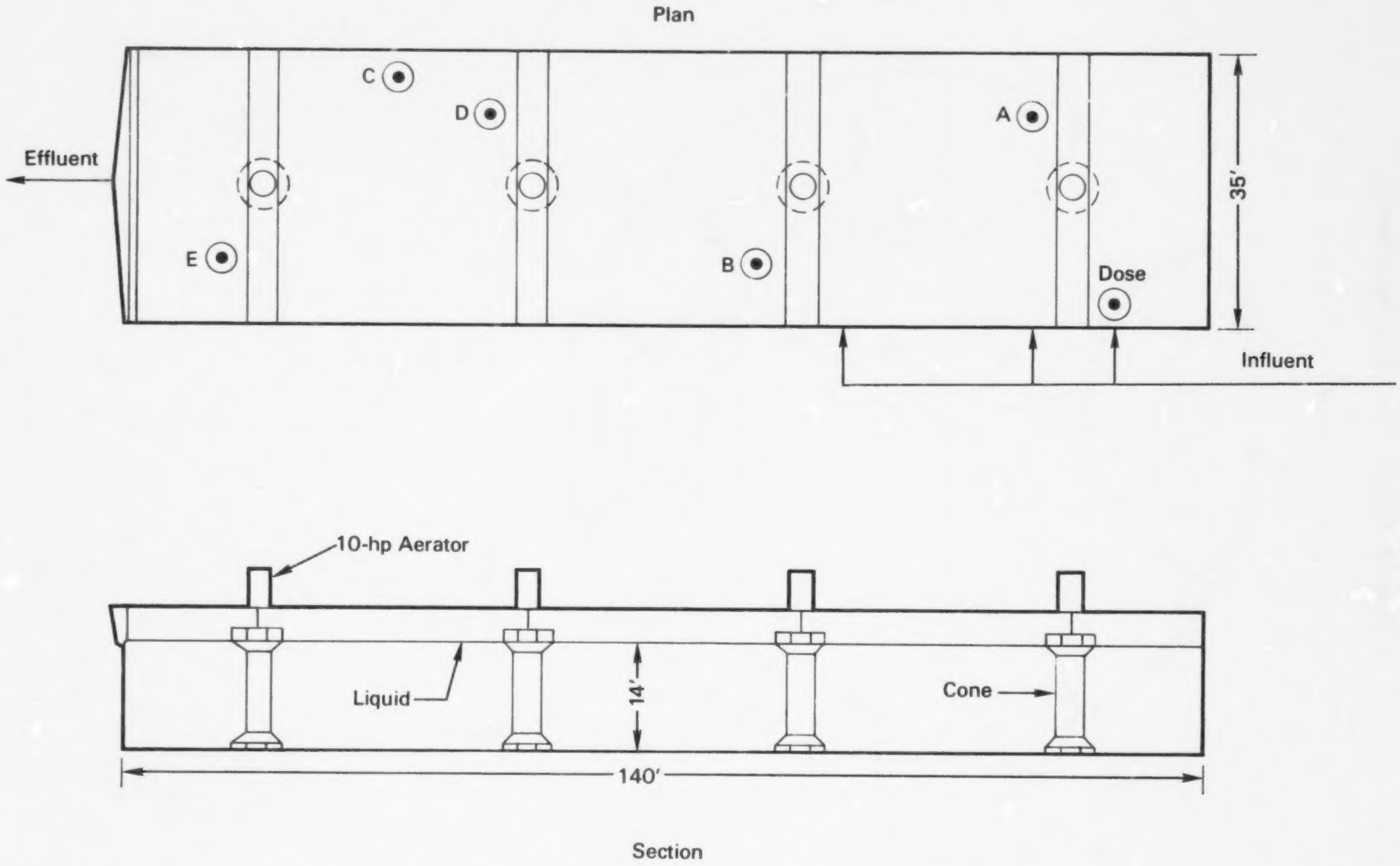
Plan
C
D
A
Effluent
35′
E
B
Dose
Influent
10-hp Aerator
Liquid
14′
Cone
140′
Section

Figure 4. South River Plant (1 and 2) Sampling Locations

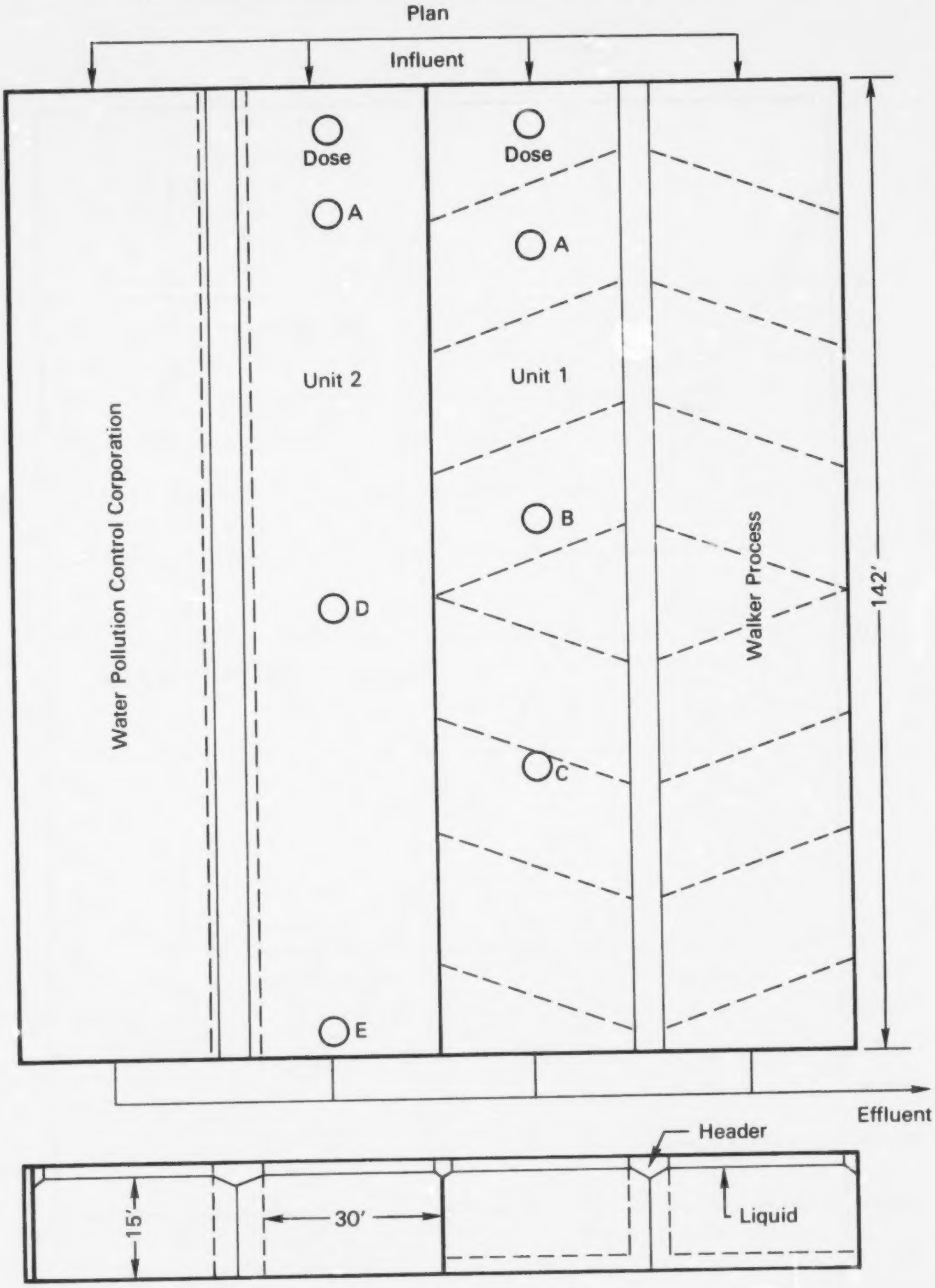
Plan
Influent
Dose
Dose
A
A
Unit 2
Unit 1
B
D
C
E
Water Pollution Control Corporation
Walker Process
142′
Effluent
Header
15′
30′
Liquid
Section

Figure 5. Kr^{85} to H^3 Concentration Ratio vs Time for Kenwood Plant (Test No. 2, Sta. A)

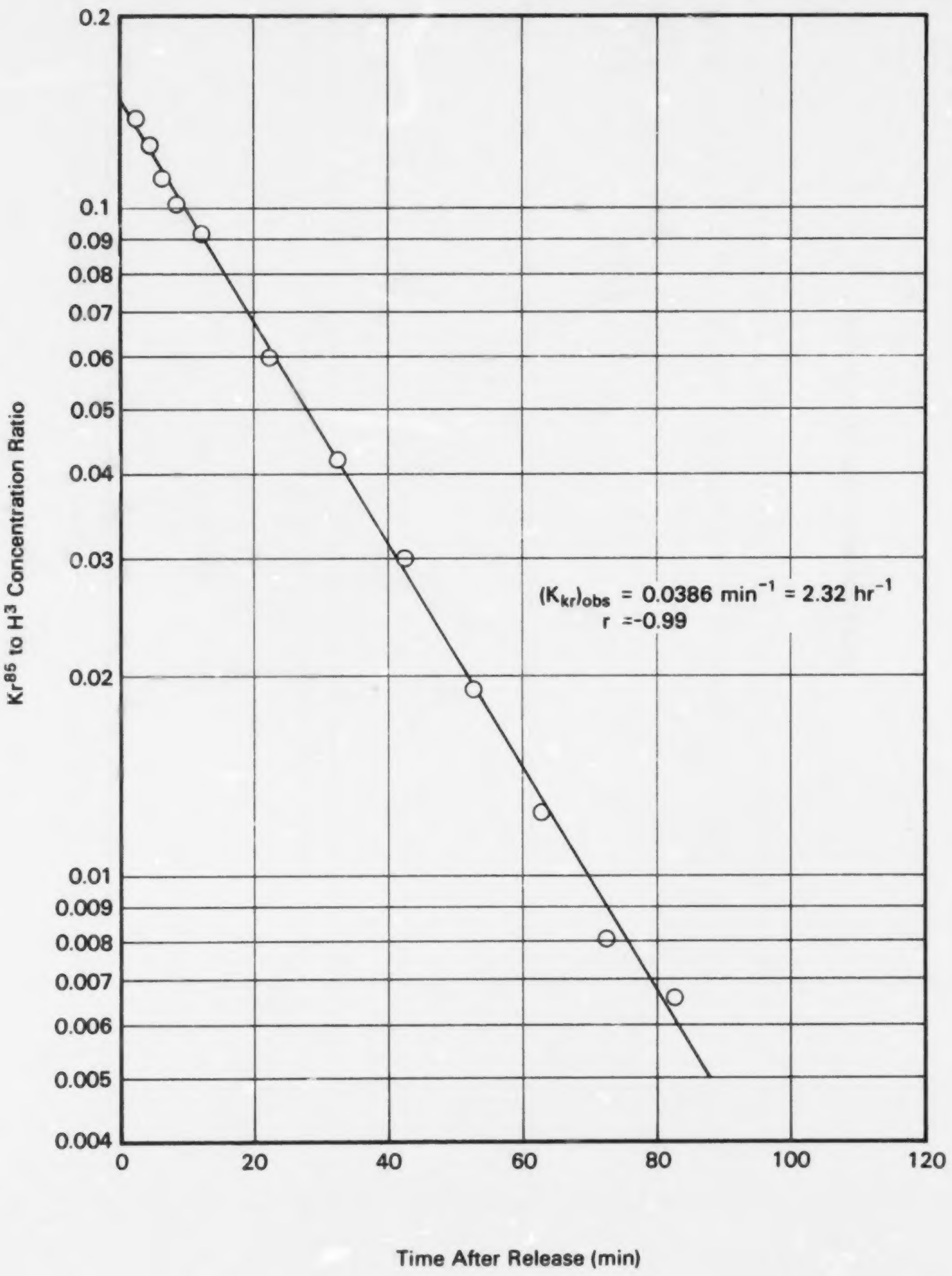

Figure 6. Kr^{85} Concentration Ratio vs Time for South River Plant (1) (Test No. 7, Sta. B)

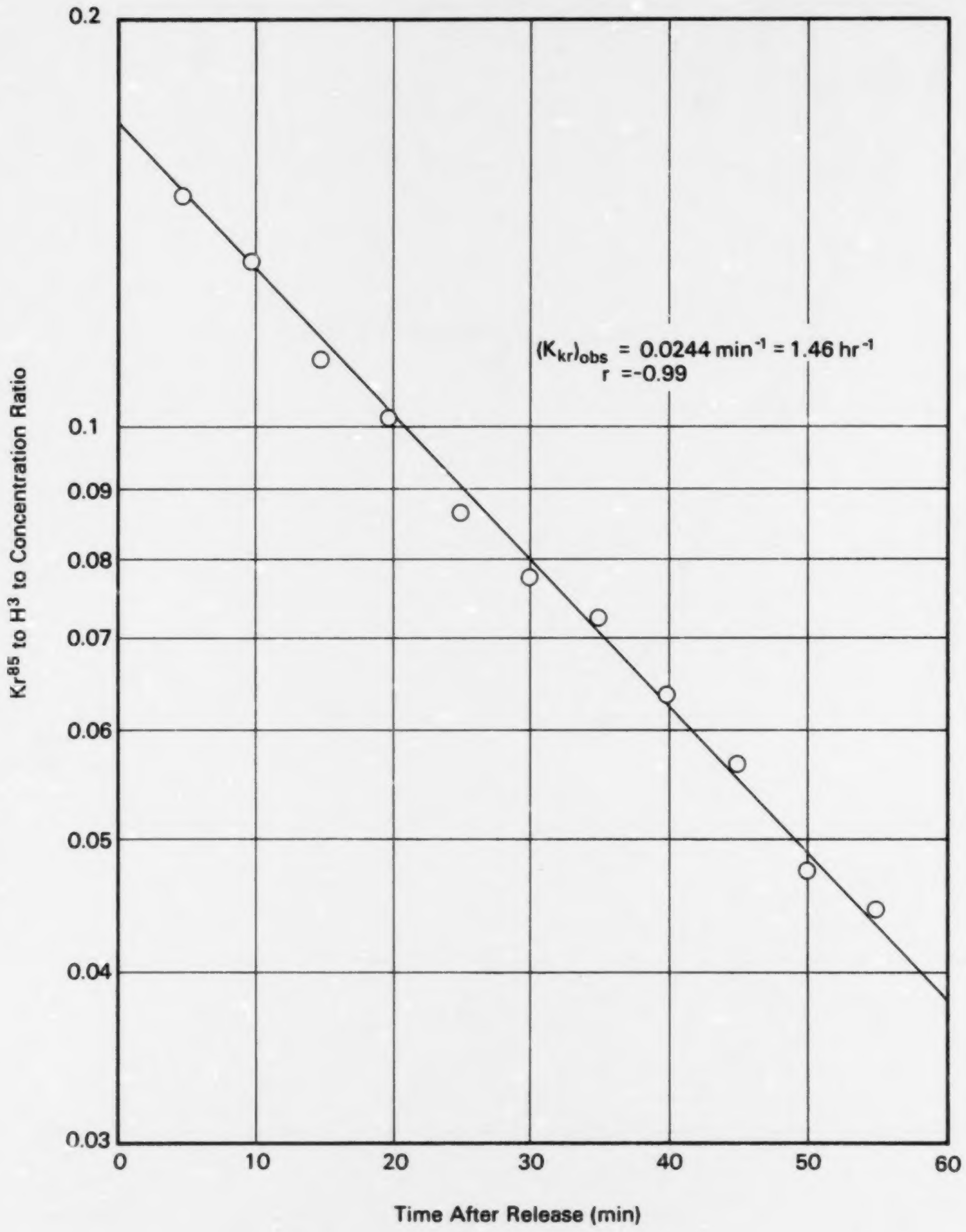

Figure 7. Kr^{85} to H^3 Concentration Ratio vs Time for South Cobb Plant (Test No. 5, Sta. B)

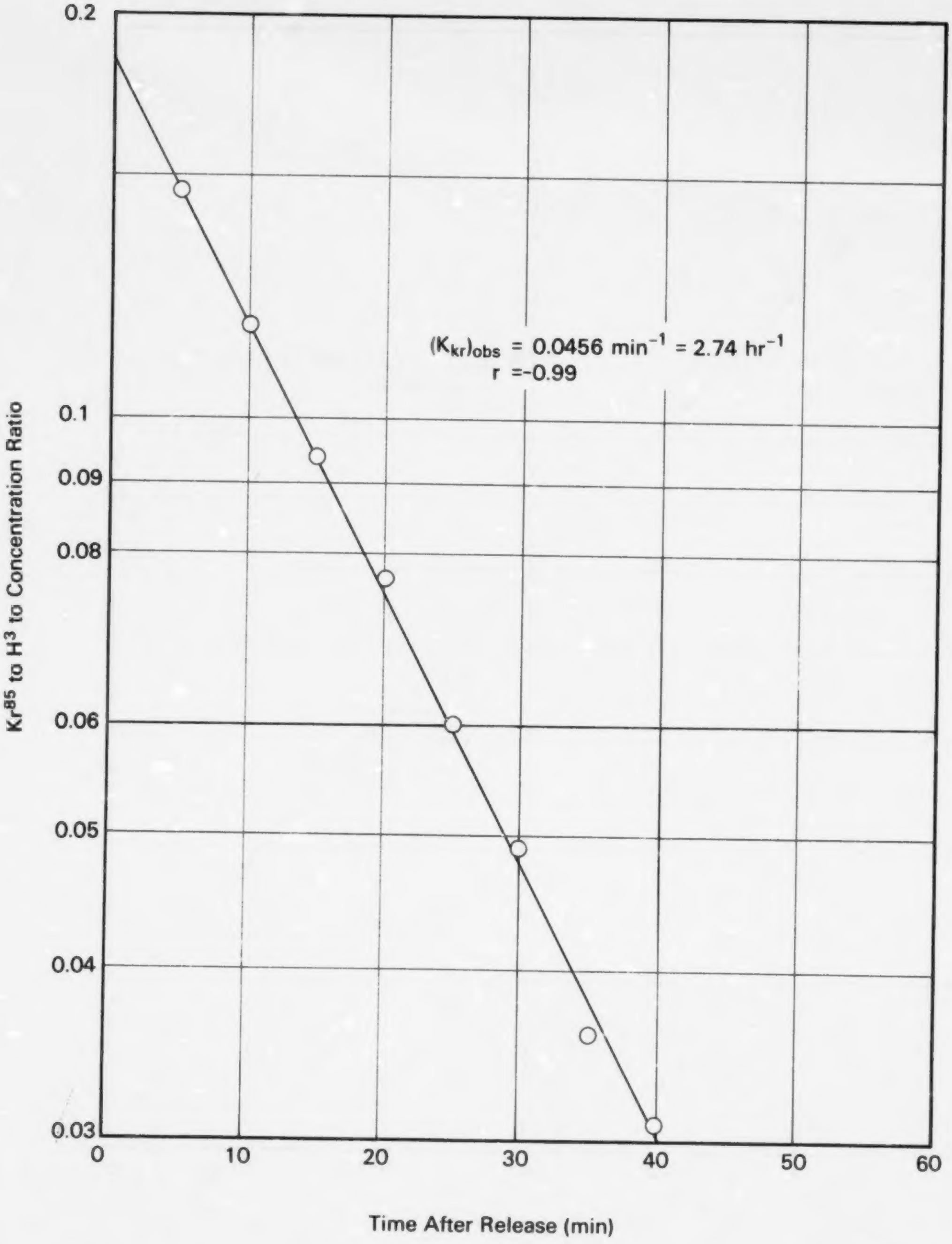

Figure 8. H^3 vs Time Curves for South Cobb Plant (Test No. 6)

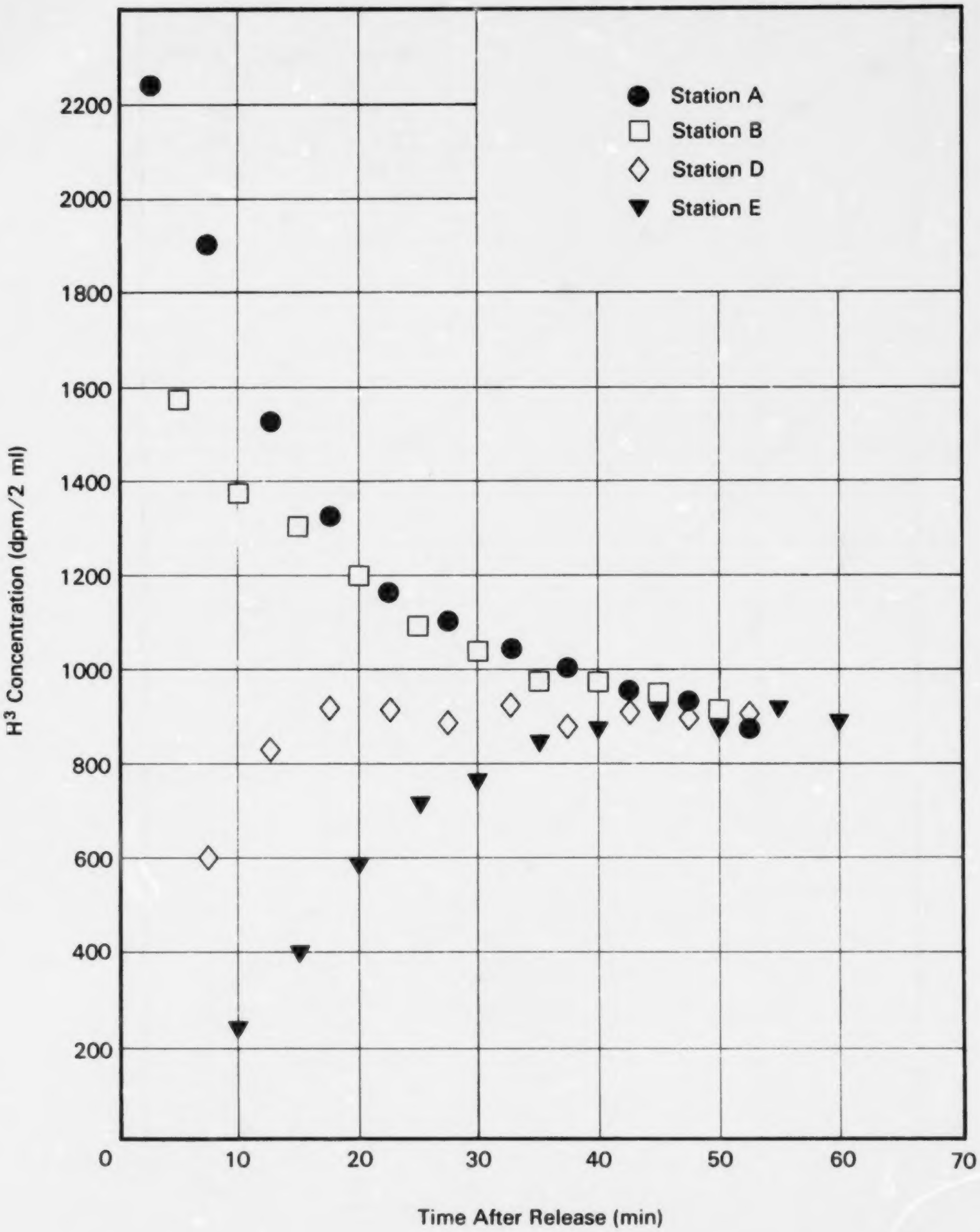

Figure 9. Kr^{85} to H^3 Concentration Ratios vs Time for South Cobb Plant (Test No. 6)

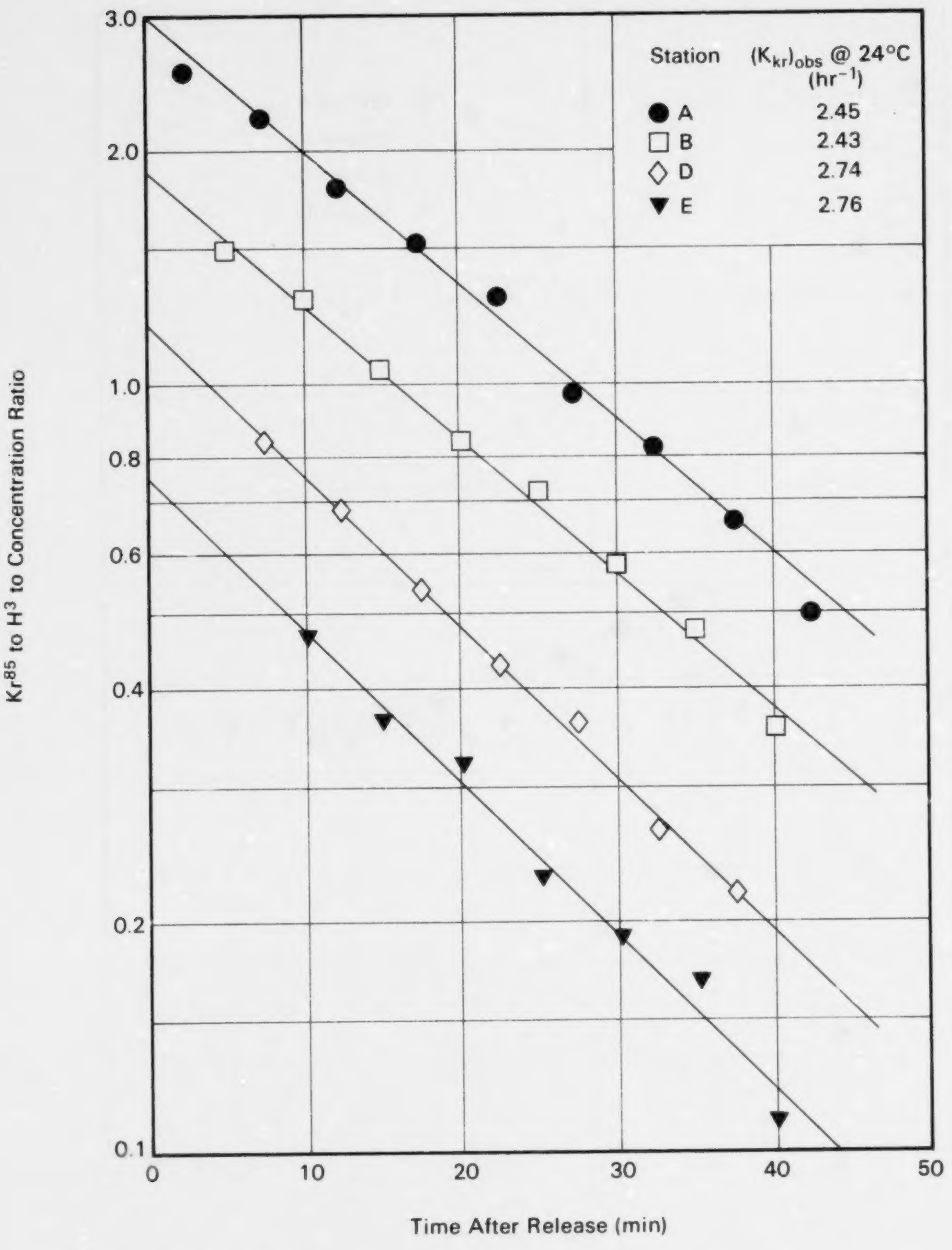

Table 3. Summary of Field Test Results — Data from Neal & Tsivoglou (6)

Plant	Date	Test Number	Station	Water Temperature (°C)	$(K_{kr})_{obs}$** (hr^{-1})	$\alpha(K_La)_{20}$*** (hr^{-1})
Kenwood*	6/25/71	1	A	25	2.21	2.39
		1	B	25	2.28	2.46
	7/2/71	2	A	23	2.32	2.62
		2	B	23	2.36	2.66
	7/9/71	3	A	23	2.50	2.82
		3	B	23	2.47	2.79
South Cobb†	7/15/71	4	A	24	2.51	2.78
		4	A	24	2.62	2.90
		4	C	24	2.91	3.22
	7/22/71	5	A	24	3.01	3.32
		5	B	24	2.74	3.02
		5	D	24	2.77	3.05
		5	E	24	2.60	2.87
	8/4/71	6	A	24	2.45	2.71
		6	B	24	2.43	2.65
		6	D	24	2.74	3.03
		6	E	24	2.76	3.05
South River 1‡	8/16/71	7	A	25	1.49	1.61
		7	B	25	1.46	1.58
		7	C	25	1.37	1.48
South River 2‡	8/24/71	8	A	25	1.37	1.48
		8	D	25	1.39	1.50
		8	E	25	1.37	1.48

* See Figure 2 for station locations.

† See Figure 3 for station locations.

‡ See Figure 4 for station locations.

**Used interchangeably with αK_{kr}.

***$\theta = 1.022$

Sta.	$\alpha(K_La)_{20}$ (hr^{-1})	Solids (ml/l)
A	2.71	550
B	2.65	560
D	3.03	460
E	3.05	440

In this one test, for a 1% increase in settleable solids, there was an approximate 0.5% decrease in the tracer-measured $\alpha(K_La)_{20}$ value. It is concluded that the variations in transfer rates observed in the South Cobb basin are real and probably reflect variable solids concentrations within the basin. It is also possible that some of the variations in $\alpha(K_La)_{20}$ reflect mixing patterns that are not uniform throughout the basin.

The two South River studies were conducted in parallel aeration basins with different diffusers in each. The South River 1 basin was equipped with Walker Process diffusers, and the South River 2 basin was equipped with Water Pollution Control Corporation diffusers. The two tests were conducted on different days, but the plant operator reported essentially identical flow, DO, and settleable solids (100-110 ml/l) for each test date.

The average $\alpha(K_La)_{20}$ value for the South River 1 basin was 1.56 hr^{-1} with a range from 1.61 hr^{-1} at the influent end of the basin (Station A) to 1.48 hr^{-1} at Station C (see Figure 4). This pattern of higher gas transfer rates at the influent end was consistent with visual observations of greater mixing intensity at the influent end. The average $\alpha(K_La)_{20}$ value for the South River 2 basin was 1.49 hr^{-1} with little variation (1.48 hr^{-1} to 1.50 hr^{-1}), and mixing intensity was visually uniform.

Because the two South River tests did not use the same sampling locations (see Figure 4), it is not possible to draw firm conclusions about relative transfer performance. A more direct comparison could be made by simultaneously testing both basins and using identical sampling stations. From the limited test results available, it is estimated that the overall oxygen transfer rate coefficient for the two systems was about the same. However, the South River 1 basin had a spatial distribution of transfer (higher at influent end) while the South River 2 basin exhibited rather uniform transfer rates.

Discussion

The field tracer technique described here was designed to measure aeration performance in any aeration system. The tracer method is a direct measurement of gas transfer under existing hydrodynamic conditions and does not depend on biological or chemical parameters as a test basis.

The tracer method is not limited to use in complete mix systems because the conservative tracer (tritiated water) accurately accounts for the effects of dispersion and dilution. As the tracer method is not limited to complete mix systems, it can also be used to evaluate aeration performance in large aerated lagoons widely used for wastewater treatment.

Conclusions

The following conclusions have been reached as a result of the studies presented here and other applications of the tracer method.

1. The gaseous tracer method can be used to determine the efficiency of gas transfer equipment in test tanks or in operating wastewater treatment facilities.
2. The field tracer studies of gas transfer performance have demonstrated that the method is practical and safe, and that the results are precise and reproducible.
3. The gaseous tracer method can be used to study the effect of pollutants on gas transfer (α).
4. Use of the gaseous tracer method can allow accurate full-scale compliance testing of oxygen transfer systems after they are installed in the field.
5. The precision of the tracer method can allow for field tests of equipment installation patterns in typical aeration basins.

Recommendations

The tracer method is intended for use where more conventional testing methods are not applicable or sufficiently accurate. The tracer method can be applied in operating aeration systems without modification or upset of normal plant operations to accurately measure aeration performance under real operating conditions.

Because of its general applicablity and accuracy, the tracer method can be used as a referee method in cases of dispute and it can also be used as a reference method in evaluating other testing techniques.

For new designs, engineers can specify aeration performance and have accurate field testing conducted after the system is in operation. For existing systems that are not now functioning as desired, the tracer method can be used to accurately determine the operational capacity of existing equipment before designing additional aeration facilities.

Tracer studies in operating aeration basins should also include measurement of alpha and characterization of basin contents with respect to physical, chemical, and biological parameters. This concurrent data collection would identify operating conditions under which the measured aeration performance was achieved. As accurate field test results become available, the U.S. Environmental Protection Agency could develop and maintain an aeration performance register that would provide all basic data from each test. With the availability of accurate and reliable field performance data, aeration design specifications can be more precisely established and the goal of energy-efficient systems can be realized.

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Section VI. Supplementary Papers

Although not planned in the original program, the following papers were presented, in part, at the Workshop.

Notes for Workshop Toward an Oxygen Transfer Standard

24

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Having been associated with oxygen transfer studies on and off for over 20 years and most recently having investigated the mixing and transfer capabilities of a number of aeration devices functioning in paper industry wastewater, I probably join with many others in questioning the need for a standard at a time when biological treatment is *fait accompli* in many industries and municipalities.

It would appear that oxygen transfer into sewage and industrial wastes is a simple straightforward, well-studied function. Most responsible manufacturers have recognized that and provide guarantees based on particular applications. Guarantees have been provided in terms of BOD removal which is the major end product of aeration, albeit the one which carries the most unknowns and risk or the bidder. It happens, however, to be the best common denominator for the paper industry, and I suspect for other users of the equipment as well. The key to its use is sufficient knowledge of the wastewater which requires treatment and the aeration system employed.

We have represented at this conference the standard confrontation between user and supplier which is often resolved by price, maintenance history, and other conditions which are not being directly addressed in this Workshop. Even if successful in specifying the standard conditions for testing and rating oxygen transfer units or systems, it remains as just one element in the user's evaluation of alternate systems. As power costs continue to rise, it becomes a more important element. However, we should ask the Society if it embarks on this venture to also consider standards for evaluating additional engineering features such as materials corrosion, motor and gear reducer stress and expected life, ice, frost, fog, foam, and odor generation. In short, the user should be in a position to judge the equipment offered by a manufacturer using as many standardized elements as possible. The challenge here is to first truly evaluate the contribution which would be made in standardizing oxygen transfer testing (which would also allow the development of new and improved transfer devices) and, second, to consider system and other engineering parameters which are often of equal or greater importance to the user.

I've made a few calculations relating oxygen transfer for a 500 ton per day paper mill. I think the answers will justify my interest in knowing if a 10% transfer difference exists between aerators A and B, all else being equal. First, industrial power costs vary considerably across the USA. Hydropower in the Pacific Northwest may cost as little as \$0.005/kW·hr. Oil generated power in the Northeast could cost \$0.05/kW·hr. Thus, the cost for oxygen transfer for the example mill would vary between \$0.18-\$1.80/ton of product or 0.23-2.3 cents/lb of BOD removed. For the mill using aerator A or B with a 10% transfer difference would mean a savings of between \$3,400-\$34,000 per year. That's equivalent to \$10-\$100 daily and is certainly significant at the higher power cost.

I believe the paper industry would like to know if a transfer difference of 10% or more exists between units and more importantly what turndown strategies can best be utilized in full-scale systems. Therefore, I'd like to bring the attendees up-to-date on our studies dealing with aeration systems and how aeration is currently deployed in systems treating papermill effluents.

Workshop Presentation

Much of the information is contained in published articles or reports (1)(2)(3). New material assembled for this Workshop consists of reviewing the types of aeration configurations currently being operated in the paper industry (Table 1), the percentage of fixed and mounted aeration (Table 2), the distribution of aerator manufacturers and types (Table 3) currently deployed in 50 paper industry systems, and the dimensions of some of the industry's deepest and shallowest systems which use conventional surface aerators (Table 4).

The performance of full-scale systems involves tank geometry selected for any number of reasons. NCASI (1) presented the hydraulic influence of various types and

Table 1. Breakdown of Aeration Basin Configurations Utilized in Biological Treatment of Paper Mill Effluents

Lines of Cells in Parallel	No. of Cells in Series in Each Line: 1	2	3	Total
1	27	12	4*	43
2	9	0	0	9
3	2	1	0	3
Total	38	13	4	55

*All oxygen systems.

Table 2. Comparison of Fixed Mounted and Floated Aeration

Plant Type	Mount	Nameplate Horsepower	% Total
ASB*	Float	36,900	70
ASB	Fixed	8,220	16
AST**/AIR	Fixed	6,310	12
AST/O_2	Fixed	1,180	2
Total		52,610	100

*ASB — Aerated Stablization Basin.
**AST — Activated Sludge Treatment.

sizes of surface mechanical aerators operating with and without sidewall restraints. These results are applicable in most wastewaters. We also showed that for low-rate systems an oxygen mass balance could be made around individual aerators using measurements of DO increase through the spray, velocity and direction measurement of the resultant flow, and wire horsepower. Furthermore, for papermill wastewaters, these transfer efficiencies were compared to overall system efficiency using: (1) oxygen uptake measurements and associated volumes of basin and (2) overall BOD_5 removal obtained in systems with sufficient (and, therefore, nonlimiting) residual BOD_5.

Using system performance based on BOD removal, it appears that oxygen transferred in the high-rate activated sludge systems is identical to that determined for the lower-rate aerated stabilization basin (ASB) systems. Information contained in NCASI's data bank was extracted to show average aerator performance in activated sludge and ASB systems (Figure 1) and ASB geometry and power levels (Figure 2) which represent 1976-77-78 performance for a large number of installations.

Table 3. Distribution of Aeration* Power Among Manufacturers for 50 Paper Industry Wastewater Treatment Systems

Aerator**	Nameplate Horsepower: Total	% Total
H3	10,040	20
L4	9,950	20
L2	9,650	19
H1	6,520	13
L3	5,060	10
H5	4,535	9
L7	2,070	4
L1	1,040	2
L6	480	1
H4	450	1
S1	300	1
L5	150	—
H2	80	—
S2	20	—
Total	50,345	100

Distribution by Type

Type	% Power
Low Speed (L)	56
High Speed (H)	43
Stationary (S)	1

*Excludes oxygen systems.
**Aerator designations listed refer to manufacturer identification.

Table 4. Physical Characteristics of Selected ASB Systems

System	Area (acres)	Avg. Depth (ft)	Size Aerators (hp)
5 Deepest	20	25	40
	16	21	75
	9	21	75
	70	20	150
	40	20	60
5 Shallowest	33	6	100
	76	7.5	60
	13	8	30
	115	8	50
	154	8	100
Average	27	13	—

Figure 1. Average Efficiency of Activated Sludge and Aerated Stabilization Systems as a Function of Aeration Capacity

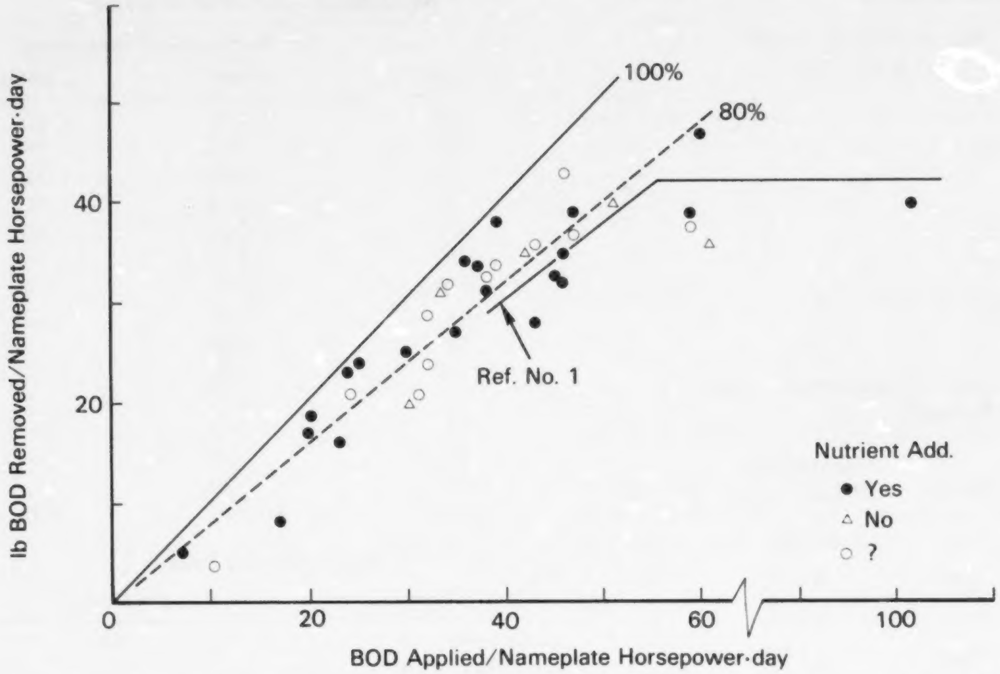
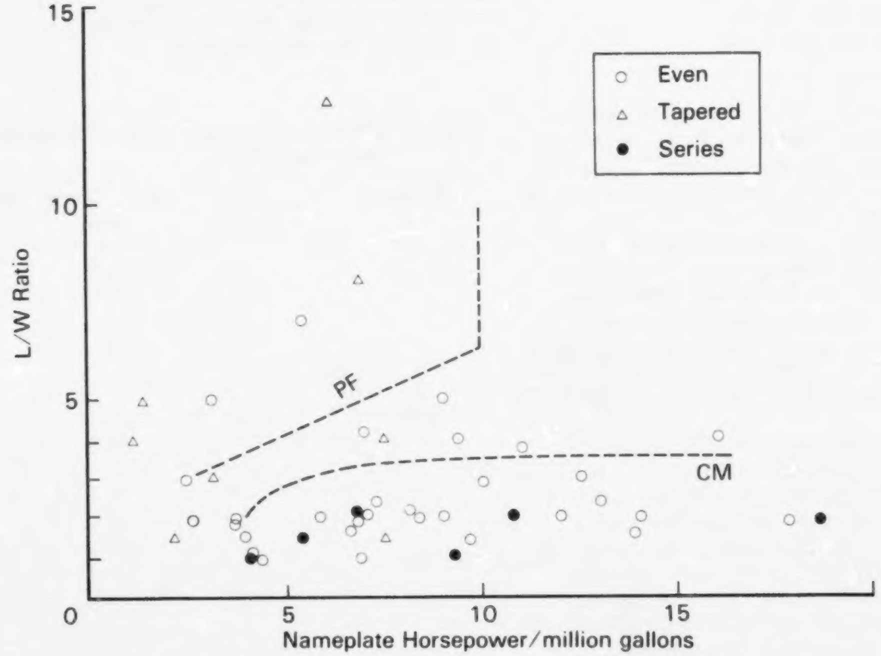


Figure 2. Mixing Regimes of Paper Industry Aerated Stabilization Basins Operating in 1978 Based on Basin Geometry, Power Level and Its Distribution



Completely mixed (CM) is defined as equal levels of BOD_5 and TSS throughout the basin. Plug flow (PF) represents a gradual reduction in BOD_5 throughout the basin. The intermediate zone in the figure contains higher BOD_5 levels at the head end only.

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Oxygen Transfer in the Activated-Sludge Process

25

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Introduction

Dissolved oxygen is essential for the aerobic biological treatment of waste waters. The activated-sludge process is designed to increase the rate of biochemical oxidation by bringing the waste water into contact with a high concentration of micro-organisms in the presence of DO. Given ideal conditions for the growth of the micro-organisms, there is a maximum rate at which the impurities in a waste water can be biochemically oxidized which is related to the physical and biochemical properties of bacterial cells; it cannot be exceeded simply because DO is readily available. It is possible to increase the rate of treatment per unit volume of the aeration tank by increasing the concentration of micro-organisms in the mixed liquor, provided that the necessary quantity of DO is made available. However, the concentration of micro-organisms can only be increased if they can be adequately concentrated by settlement and recycled.

The total quantity of oxygen required per unit time can be calculated from the BOD of the waste water, its concentration of ammoniacal nitrogen, and the rate of treatment required (g BOD removed/g aerated activated sludge·day). However, the maximum rate at which oxygen can be dissolved (kg/m^3 aeration-tank capacity·hr) depends on the performance of the aeration system, the efficiency of which is normally judged by the rate at which oxygen dissolves and the electrical energy required.

Traditional methods of aeration — largely confined to diffused-air and mechanical surface systems — have been used successfully for many years in the UK to provide the oxygen required by micro-organisms in lightly loaded treatment plants designed to achieve effluents of high quality. Factors affecting the performance of such systems of aeration have been investigated by many research workers (4)(5)(6)(8)(9)(10)(13)(14), although only recently have the full importance of tank geometry and the relation between aeration intensity and degree of purification achieved been realized.

Oxygen Requirements

Total Quantity

The amount of DO required for treatment of a waste water will depend on the total oxygen demand of the micro-organisms oxidizing both carbonaceous and nitrogenous matter. Data obtained for full-scale conventional plants have shown (16) that the quantity of oxygen required for oxidation of carbonaceous matter is approximately equal to the BOD of the waste water, assuming nitrification has been inhibited in the BOD test. When full nitrification is necessary, and facilities are not available to utilize oxygen from nitrate by controlled denitrification, the additional amount of oxygen required can be calculated by assuming that each gram of ammoniacal nitrogen requires approximately 4.25 g of oxygen for conversion partially to nitrate and partially to bacterial cells. To ensure that the rate of nitrification will not be limited by lack of DO, the concentration of DO in the mixed liquor should be maintained near to the optimal value of 2 mg/l.

The amount of oxygen required for oxidation of carbonaceous matter will be affected by the rate of sludge loading (g BOD/g sludge·day). At high loadings ($>$0.6 g/g·day), more of the organic matter will be converted into bacterial cells than when the loading is low ($<$0.1 g/g·day). The oxygen requirement varies from about 0.8 to about 1.6 times the BOD of waste water, corresponding to sludge loadings of 1 to $<$0.1 g BOD/g·day, respectively; at a conventional sludge loading, corresponding to a sludge age of about 5 days, the oxygen requirement is equal to the BOD of the waste water.

Not all of the oxygen used to oxidize ammonia need be lost as nitrate in the final effluent. In anoxic conditions, that is in the absence of DO, micro-organisms in activated sludge will utilize nitrate for oxidation of carbonaceous matter; the nitrate is reduced in the process to elementary nitrogen. Recent experiments have shown that about 50% of the nitrate in the effluent from a plug-flow activated-sludge plant can be utilized by providing an anoxic region at the inlet end of the aeration tank (3). The required anoxic conditions were achieved when equal flows of recycled sludge and settled sewage were mixed, but inadequately aerated, for about 40 min. By reducing the intensity of aeration to produce a second anoxic region, with a 30 min retention period, at about the mid-point of a similar plug-flow aeration unit and feeding 40% of the settled sewage to this region, denitrification increased to about 70%. To obtain the benefits of denitrification, and thus reduce the total quantity of oxygen to be supplied by the aeration system, the rate of input of DO and the concentration of micro-organisms in the aerobic regions must be sufficient to achieve full nitrification.

Rate of Demand

The total quantity of dissolved oxygen required daily can be calculated as described above, but the rate (kg/m^3 mixed liquor·hr) at which oxygen has to be introduced by the aerator (aeration intensity) to maintain aerobic conditions is directly related to the rate of biochemical oxidation in the mixed liquor. This depends on the period for which the waste water is aerated, the input of BOD, the concentration of suspended solids in the mixed liquor, and the temperature of the mixed liquor.

The rate at which oxygen is consumed in an aeration tank depends on variables such as the treatability of the waste water (influenced by changes in pH value, the presence of substances which may inhibit biochemical oxidation, and the biodegradability of each constituent substance), the population of micro-organisms, the concentration of DO, and the temperature. Some of these variables may approach critical limits. A relation between specific rate of removal of BOD (g BOD removed/g activated sludge·day) in an activated-sludge plant and BOD of effluent has been obtained (Figure 1) at the Water Research Centre, using pilot plants treating a constant flow of settled domestic sewage of average BOD (250 mg/l) at temperatures of about 15°C. Diurnal variations in flow, which caused variations in sludge loading, were shown to affect effluent quality rapidly (1), although the aeration intensity was adequate at all times. For design of an aeration system, the maximum sludge loading expected during the retention period of mixed liquor in the aeration tank should be used if it is required that the standard of effluent be maintained consistently.

Hines *et al* (11) have claimed that the maximum rate at which oxygen would be required by high concentrations of micro-organisms, treating a readily biodegradable organic substrate under ideal conditions in a fermentation unit, is approximately 10 kg/m^3 aeration-tank volume·hr. However, from an activated-sludge plant, it is generally necessary to produce an effluent of low BOD (<20 mg/l); the rate of biochemical oxidation is therefore limited by available substrate and the concentration of micro-organisms by the capacity to settle and recycle the sludge. Data shown in Figure 1 indicate that, when the BOD of the effluent was 20 mg/l, the rate of biochemical oxidation was only 1 g/g·day. To dissolve oxygen at that rate would require an aeration intensity of only 0.2 kg O_2/m^3·hr, assuming that the concentration of mixed-liquor suspended solids was 5000 mg/l and no additional oxygen was required for nitrification. This intensity of aeration was easily achieved in the laboratory with a fine-bubble diffused-air system, passing air at a high rate through closely spaced diffusers at the bottom of a shallow (0.6 m) tank, and frequently cleaning the external surfaces of the diffusers. The maximum intensity normally achieved with full-scale conventional aerators is about 0.1 kg/m^3·hr and this may limit the maximum rate of treatment.

The rates at which carbonaceous and nitrogenous oxidation occur are affected by changes in temperature of the mixed liquor. Studies at the Water Research Centre (1)(2)(7) have shown that both rates increase as the temperature increases, and approximately double for an increase from 7 to 17°C. However, long-term operation of a plant at low temperatures may result in an increase in the proportion of viable bacteria in the activated sludge, thus increasing the total quantity of active enzymes present and a gradual increase in temperature from 7 to 17°C over a period of several months might not then result in a doubling of the rate of oxidation. Rapid diurnal changes in temperature of the mixed liquor will affect both rates of oxidation, and the intensity of the aeration system should be based on the maximum rates anticipated, preferably as determined by using a respirometer or pilot plant fed with sewage at the appropriate temperature.

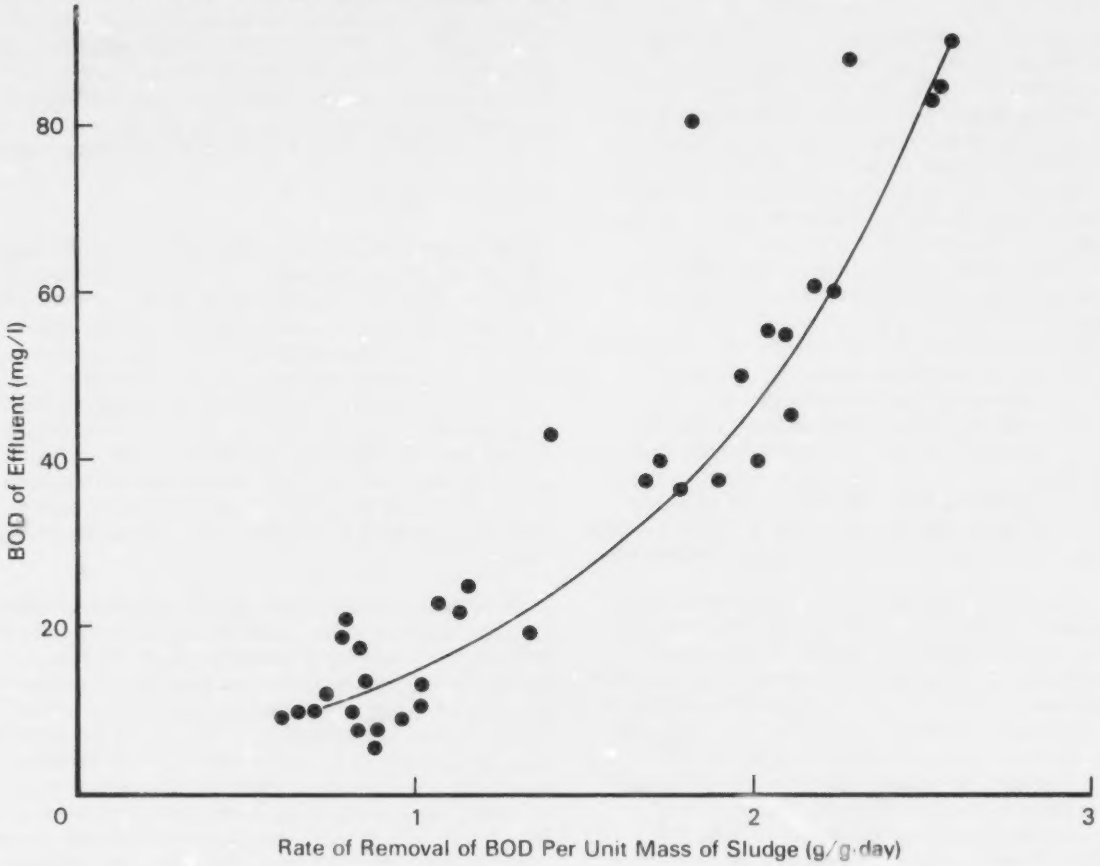
Oxygen Supply

DO is conventionally supplied to the activated-sludge process by either diffused-air or mechanical surface aeration. Many different types of diffused-air systems have been developed, using a variety of diffusers — ceramic or compressed-plastics diffusers, spargers, drilled pipes, or expanded plastics — to produce bubbles of various sizes. Generally, air is diffused into the liquor near the bottom of the tank so as to achieve adequate mixing and the maximum contact time for aeration. There are only two basic types of mechanical surface aerators, differentiated by the plane of rotation which may be vertical (cone type of aerator) or horizontal (brush type of aerator).

Two commonly used criteria of performance of aeration equipment are aeration capacity and aeration efficiency. The aeration capacity is defined (13) as the rate of absorption of oxygen during aeration of completely deoxygenated liquid at a specified temperature (either 10°C or 20°C). Aeration efficiency is defined as the aeration capacity per unit of energy supplied to the aeration system at a given temperature. In all types of aeration systems, the mass-transfer coefficient increases by about 2% of the value at 10°C per degree-centigrade increase in temperature in the range 0 to 30°C. As the value of the saturation concentration decreases with increasing temperature by about the same amount, the effect of changes in temperature on the aeration capacity or efficiency can be neglected unless extreme accuracy is required (4).

The ability of an aerator to dissolve oxygen is normally measured indirectly by conducting tests in clean water using the non-steady-state method described elsewhere (4)(13). Reproducible results can be obtained by these tests, provided they are carried out under carefully controlled conditions. This method is not wholly satisfactory because clean water can easily become contaminated by traces of surface-active substances which affect the rate

Figure 1. Relation Between Rate of Removal of BOD of Settled Domestic Sewage Per Unit Mass of Activated Sludge and BOD of Final Effluent



of oxygen transfer (4)(10)(13)(14). To minimize the effect of possible contamination (particularly when testing full-scale aerators), it has been widely accepted that aerators are best tested in tap water to which sufficient anionic surface-active material has been added to achieve an average concentration of 5 mg/l during each test. Well-purified sewage effluent or river water has been used instead of tap water in such experiments and has been found to be satisfactory (5). The rate of oxygen transfer in mixing liquor will, however, be affected by changes in such factors as degree of purification of the waste water, configuration of the aerators in the aeration tank, geometry of the aeration tank, and concentration of dissolved oxygen. These will be discussed later.

The performance of an aeration system can be measured during its operation in mixed liquor, using the steady state method (4)(13). The performance of a given system in mixed liquor is similar to that obtained in tap water to which 5 mg/l of anionic surface-active agent have been added, but is substantially different from that obtained in clean water when operated in the same tank under similar conditions. In order to relate the rate of oxygen transfer measured in clean water to that measured in mixed liquor under similar conditions, a proportionality factor, 'alpha', is used (13).

The 'Alpha' Factor and its Application

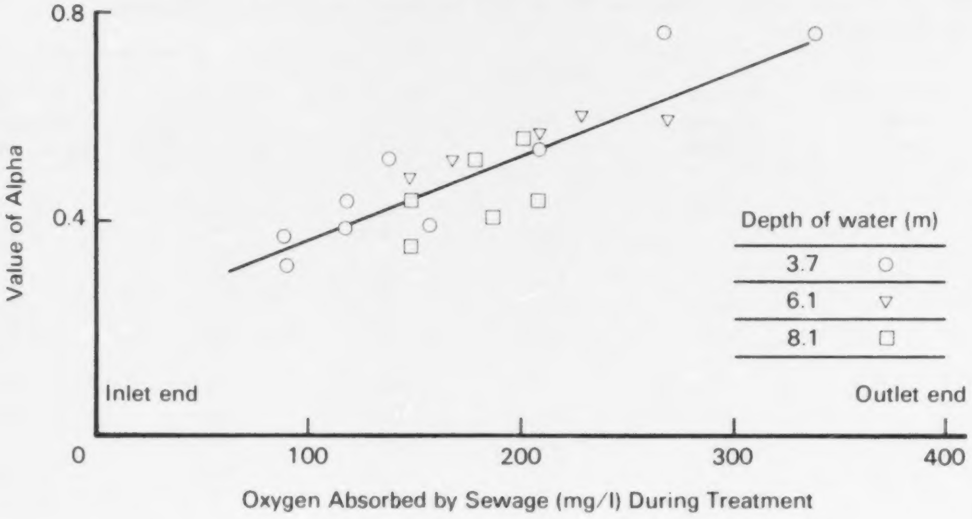
The value of 'alpha' for a given aeration system is equal to the performance of the system (aeration efficiency or capacity) in mixed liquor under specified conditions divided by the corresponding performance under similar conditions in clean water. Results of previous investigations (13) show that the performance of an aeration system in mixed liquor varies significantly according to the nature of the liquor, the contaminants present, and the intensity of aeration.

The value of 'alpha' for a given aerator does not remain constant. The efficiency of transfer of oxygen into mixed liquor varies with the degree of purification of the waste water. Experiments with a fine-bubble aeration system, operating in an essentially plug-flow unit, have shown (Figure 2) variations in 'alpha' from 0.3 at the start of the treatment (when the waste water was first brought into contact with recycled sludge) to 0.8 at completion of treatment (when a high-quality, fully nitrified effluent had been produced). The results of these experiments (13) showed that the performance of the aerator at the inlet end of the plug-flow unit was similar to that measured in tap water with 5 mg/l of added detergent, under similar conditions of air-flow rate, temperature, tank geometry, and diffuser configuration. The performance at the outlet end of a plug-flow unit corresponds approximately to that measured in clean water under similar conditions.

The performance of mechanical surface aerators installed in a plug-flow aeration unit also varies as the sewage becomes purified. With such aerators, the value of 'alpha' will decrease from about 1.2 initially, to a final value of about 1, corresponding to a decrease in the concentration of contaminants.

It has been postulated that a change in oxygen transfer rate as a result of the addition of anionic detergent to

Figure 2. Variation of Alpha Factor with Degree of Purification of Sewage from Inlet to Outlet of a Plug-Flow Aeration Tank



tap water is due to the physical rather than the chemical properties of the detergent; it would be expected that the addition of cationic or non-ionic detergents would have similar effects (8). It is thought that the increase in transfer rate caused by the addition of detergent to a mechanical surface-aeration system is due to a decrease in surface tension resulting in the formation of a very large number of small droplets of water, the surfaces of which are continuously reformed while they are in contact with the atmosphere. In a diffused-air system, the addition of detergent also decreases surface tension and smaller bubbles of air are produced but, unlike the effect described for surface-aeration systems where the interfacial surfaces are continually renewed, the detergent is thought also to form a stationary boundry layer at the surface of the bubble, so decreasing the rate of transfer of oxygen.

'Alpha' values do not indicate the relative efficiency of aeration systems. A diffused-air system may have an 'alpha' value as low as 0.4 and a mechanical system one as high as 1.2.

Relative efficiencies of different aerators can be judged by comparison of their mass-transfer coefficients, aeration capacities, or efficiencies, only when tested under similar conditions in mixed liquor or tap water with added detergent. For example, a diffused-air system may have an aeration efficiency of 5 kg/kW·hr in clean water and 2 kg/kW·hr in mixed liquor, whereas a mechanical surface aerator could have corresponding values of 1.5 and 1.8 kg/kW·hr, respectively.

Effect of Tank Geometry

The geometry of an aeration tank and the configuration of diffusers will affect the performance of a fine-bubble aeration system. The results of experiments carried out at the Water Research Centre (13), given in Table 1, show significant variations in performance of an aeration system over a range of depths of immersion as a result of using different numbers of dome diffusers. Increasing the number of diffusers from five to ten increased the aeration intensity and efficiency at each depth of immersion, although the total flow of air, and hence the power required, was reasonably constant. A further increase in the number of diffusers to 25, with the same nominal flow of air per dome, did not further increase efficiency. Duplicate tests at a depth of 2.4 m indicate the reproducibility of results obtained by careful measurement.

The significant effect of changes in tank geometry on the performance of a fine-bubble aeration system at depths up to 3 m has been confirmed (Figure 3) by tests carried out with similar configurations of diffusers in aeration tanks of different shape. The effect probably results from changes in patterns of mixing and hence the relative velocities of bubbles and water.

Variations in the performance of a surface aerator with changes in its size and in the geometry of the tank have not been studied extensively. Kormanik (12) claims that the rate of oxygen transfer is not affected by depth of water in the tank within the range tested (4 to 6 m), although the rate increases as the power supplied per unit surface area is increased. The rate of rotation was found to affect aeration effeciency, in agreement with earlier work by Downing *et al* (5). An increase in rate from 26 to 34 rev/min increased the efficiency by about 50%. However, this has not been borne out by some recent experiments with a full-scale mechanical surface aerator. In these, it was observed that the efficiency of the aerator was reduced by about 40% when the speed of rotation was increased from 42 to 55 rev/min and double the electrical energy was supplied. This reduction in efficiency was thought to be caused by a deflection

Table 1. Conditions and Results of Experiments with Fine-Bubble Aeration of Water Containing 5 mg/l Anionic Detergent

Depth of Still Water in Tank (m)	Volume of Water in Tank (m^3)	No. of diffuser Domes in Use	Nominal Flow of Air Per Dome (m^3/hr)	Total Flow of Air (m^3/hr)	Aeration Intensity (g O_2/m^3·hr)	Aeration Efficiency (kg O_2/kW·hr)
1.2	2.57	5	1.7	9.0	35.0	1.97
		10	0.85	9.0	45.0	2.53
2.4	5.31	5	1.7	9.5	26.2	1.46
		10	0.85	9.3	43.0	2.38
		10	0.85	9.3	42.0	2.33
4.9	10.80	5	1.7	10.4	28.0	1.57
		10	0.85	10.4	49.5	2.87
		10	1.7	20.4	69.0	1.94
		25	0.85	25.5	103.0	2.31
		25	1.7	42.1	170.0	2.34

Figure 3. Effect of Depth of Immersion of Diffusers on Oxygen-Transfer Coefficients in Different Shaped Tanks in Which Water Containing 5 mg/l Anionic Detergent Was Aerated

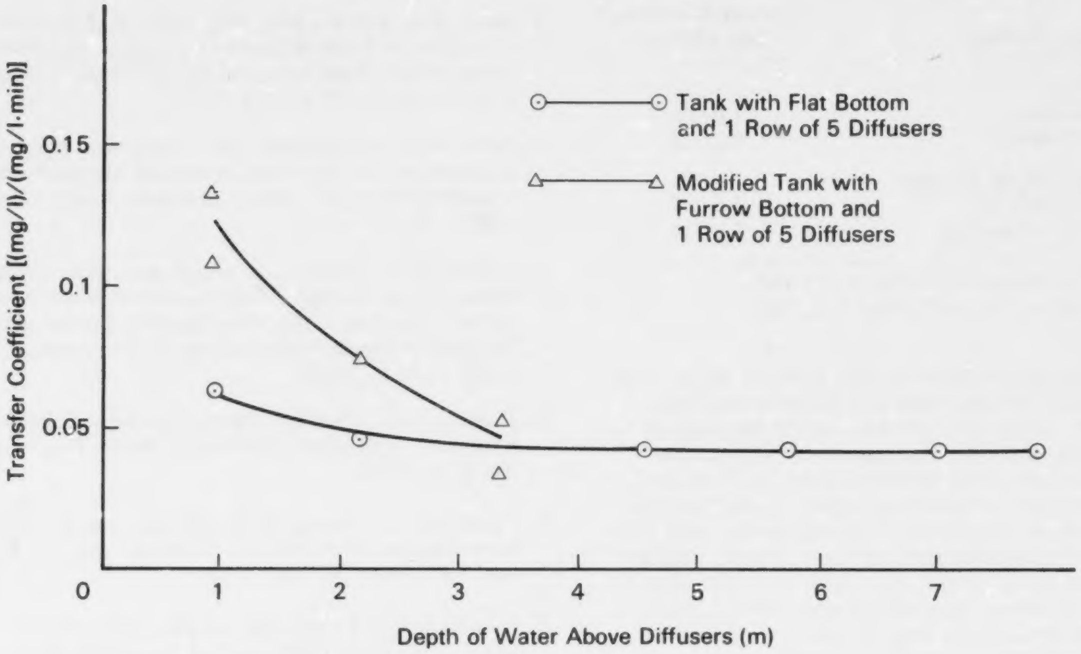


plate attached to the top of the aerator and designed to prevent liquor from being thrown out of the tank when the aerator was operated at high speed. The deflection plate prevented adequate circulation of the mixed liquor and thus wasted power.

Poor mixing of the liquor by the surface aerator at one treatment works resulted in inadequate aeration because only a small proportion of the contents near the surface of the aeration tank was recirculated and aerated. This small proportion of the liquor contained high concentrations of dissolved oxygen, which reduced the saturation deficit and hence directly reduced the rate of oxygen transfer.

An assessment of mixing and aeration by six surface-aeration systems has been made by the British Carbonization Research Association (15). They reported that mixing of the contents of an aeration tank may be inadequate with some types of surface aerator, and they have suggested methods of improving performance.

Effect of DO Concentration

It is normal for the maximum performance of an aeration system to be quoted, assuming that the system is used to aerate water containing zero concentration of DO. However, the rate of transfer of oxygen is directly proportional to the DO saturation deficit. Hence, the rate at which oxygen is transferred into mixed liquor in an aeration tank will vary, depending on the concentration of DO. For example, when nitrification is required, and hence an aerator is operated in mixed liquor to maintain consistently 20% of the saturation concentration of DO, the rate and efficiency of oxygen transfer will be 0.8 of the quoted maximum. At times of low loading, if 60% of the saturation concentration of DO was present in the mixed liquor, the rate and efficiency of oxygen transfer would be only 0.4 of the maximum.

Performance of Different Types of Aeration Systems

Because of the variety of factors which affect the performance of an aerator, comparable data for different aeration systems can only be provided by specifying the conditions under which a given liquid is being aerated and by stating the range over which the performance is likely to vary with, for example, tank geometry and aerator configuration.

The values given in Table 2 are generally applicable to most of the conventional systems available in the UK. Fine-bubble diffused-air systems include both ceramic and compressed-plastics types of diffusers, and systems for coarse-bubble diffused air include spargers, drilled pipes, and diffusers fabricated from expanded or woven plastics.

Table 2. The Aeration Efficiency of Various Conventional Aeration Systems as Measured in Tap Water Containing 5 mg/l Surface-Active Agent

Aeration System	Aeration Efficiency* (kg/kW·hr)
Diffused Air	
Fine bubbles	1.5-3.6
Coarse bubbles	0.9-1.2
Mechanical Surface Aerators	
Rotating vertically	1.5-2.2
Rotating horizontally	1.2-2.4

*Measured at maximum deficit of DO and calculated from the total power supplied.

Aeration efficiencies of the various systems, as given in Table 2, should only be used as a guide to the likely power consumption. The selection of the best system for a given situation should also take into account other factors which can affect operating costs, such as flexibility and reliability of operation, ability to maintain the micro-organisms in suspension by effectively mixing the contents of the aeration tank, and the need for regular maintenance of equipment associated with the aeration system (compressors, gear boxes, and motors). Differences in capital costs for each system should also be taken into account.

Conclusions

Conventional aeration systems provide oxygen at a relatively low rate although at a fairly high efficiency. To increase the rate at which oxygen can be suppled by conventional equipment would require installation of more equipment, which may only be fully used for short periods at times of high BOD loading. The maximum aeration intensity achieved in practice is about 0.1 $kg/m^3 \cdot hr$.

Selection of the most suitable aeration system to be used in a given situation requires detailed consideration of potential operational costs as well as capital costs. Operational costs can be directly related to the energy required to dissolve a given weight of oxygen at the required rate. Nevertheless, the ability of the aeration system to maintain the micro-organisms in suspension by effectively mixing the contents of the aeration tank and the flexibility and reliability of operation must also be taken into account in the selection of a system.

Acknowledgement

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Effect of Gas Phase Temperature on Oxygen Saturation Value

26

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Theory

Henry's Law states that the amount of gas dissolved by a given volume of liquid, at constant temperature, is proportional to the pressure of the gas with which it is in equilibrium.

This law can be expressed by the following equations:

$$C^* = Hp_{o_2} \qquad (1)$$

H = Henry's Law constant, mg/l·atm
C^* = gas concentration at saturation, mg/l
p_{o_2} = partial pressure of gas, atm

$$p_{o_2} = y(p_t - p_v) \qquad (2)$$

y = dry weight fraction of the gas in the mixture. For oxygen in air, $y = 0.209$.
p_t = total pressure, mm Hg
p_v = vapor pressure of liquid, mm Hg

$$p_t = p_b + (h/13.6) \qquad (3)$$

p_b = barometric pressure, mm Hg
h = manometer reading, mm H_2O

This study shows the effect of the gas phase temperature on the oxygen saturation value in water at constant liquid temperature for a water vapor saturated gas phase.

Apparatus

The arrangement of the equipment used in this experiment is depicted in Figure 1.

The experiment was performed in a closed system consisting of a reactor with water at a constant temperature. In order to keep the temperature of water in the reactor uniform, the reactor was placed in a large water bath with circulating tap water. An electrical heater was used in the bath to bring the water temperature to the desired value. Room air was compressed, pre-saturated, and heated prior to entry into the reactor. Gas flow was monitored in the rotameter and kept constant throughout the experiment.

The temperatures of gas and liquid in the reactor, the reactor pressure, and the oxygen concentration in the liquid phase were recorded every 5 min, as well as the air flow rate and the air temperature in the saturation flask. These data were recorded for 60 min during every run, at the end of which three samples of water were taken from the reactor to determine the final concentration using the Winkler test. This analysis provided a check of the values for dissolved oxygen read during the experiment from the oxygen meter (YSI). The barometric pressure was recorded at the beginning and end of every run. All runs were performed at constant agitator speed and torque.

Results

Table 1 summarizes the results for two runs at two different liquid temperatures. In each case the temperature of the gas was varied, and Henry's constant was calculated using Equation 1 of the theory. The gas flow and the agitator speed were kept constant at 2.6 scfm

Table 1. Results

	Measured Values									
Run	T_{liq} (°C)	T_{gas} (°C)	C^* (mg/l)	p_b (mm Hg)	h (mm H_2O)	p_v^* (mm Hg)	p_t (mm Hg) (Eq. 3)	p_t–p_v (mm Hg)	p_{o_2} (atm) (Eq. 2)	H (mg/l·atm) (Eq. 1)
1a	20	20.6	9.2	772.0	39.0	18.2	772.3	754.1	0.207	44.4
1b	20	30.5	9.0	770.3	55.0	32.7	770.7	738.0	0.203	44.3
1c	20	39.1	8.7	769.4	60.0	52.7	769.8	717.1	0.197	44.2
2a	30	21.4	7.5	758.0	44.0	19.1	761.2	742.1	0.204	36.8
2b	30	30.3	7.4	760.3	58.0	32.4	764.6	732.2	0.201	36.8
2c	30	39.1	7.4	771.4	63.0	52.7	776.0	723.3	0.199	37.2

*From Perry's Chemical Engineers' Handbook, 4th Ed.

Figure 1. Reactor

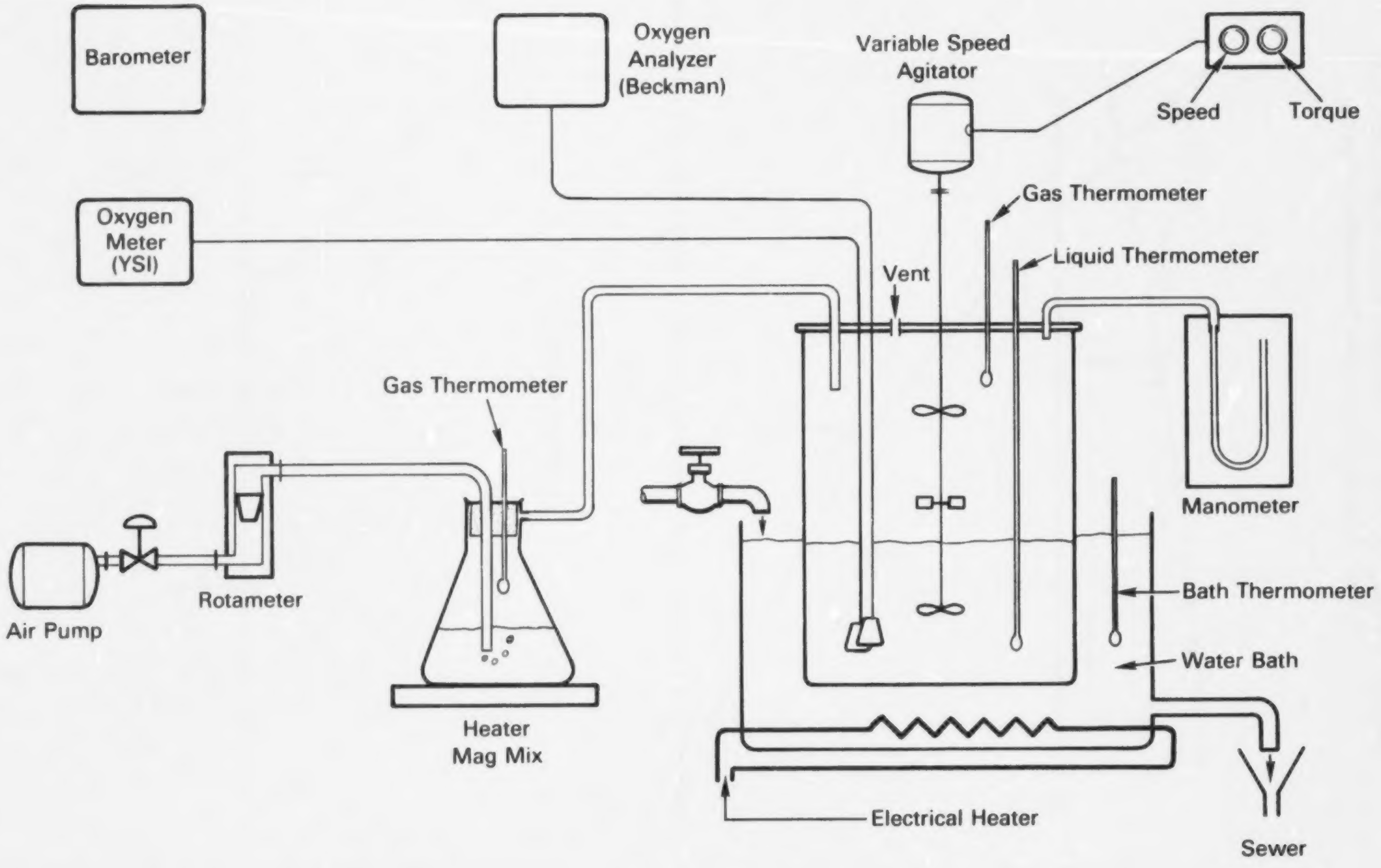
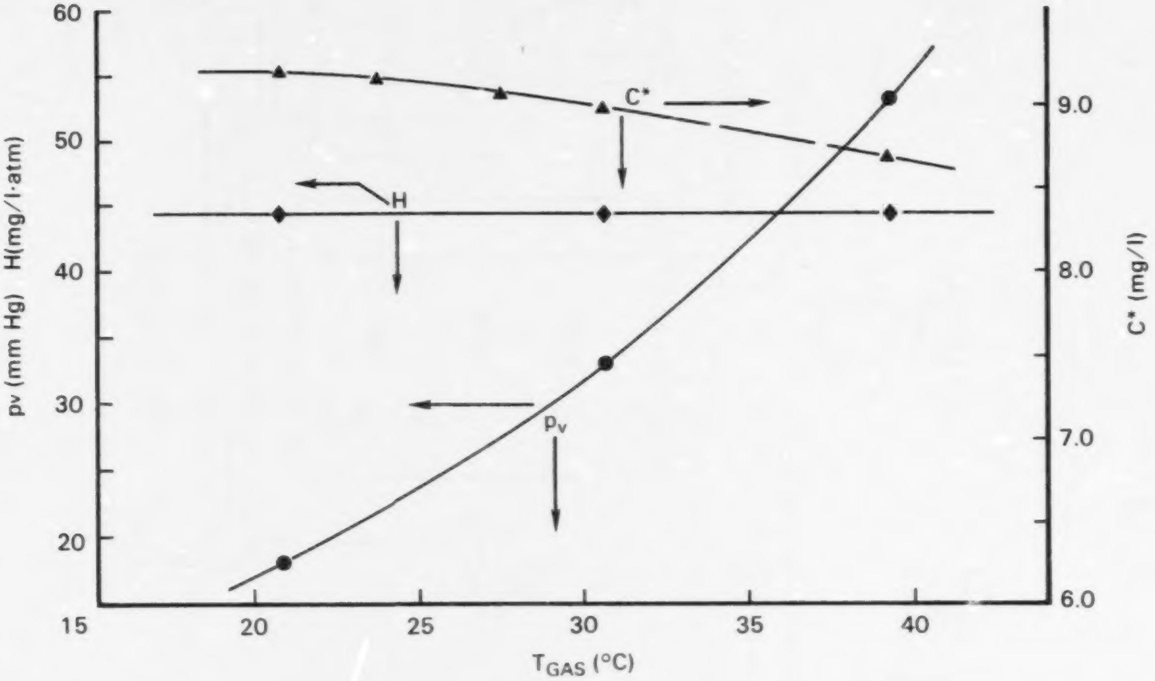
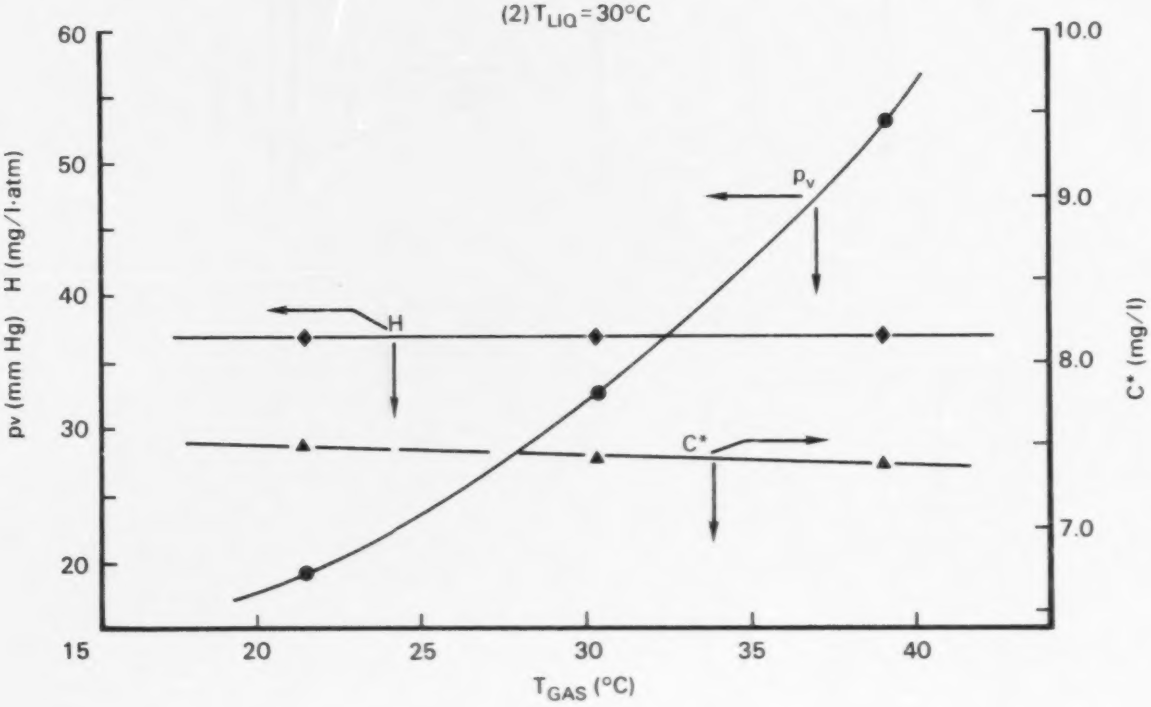

Barometer
Oxygen Analyzer (Beckman)
Variable Speed Agitator
Speed
Torque
Oxygen Meter (YSI)
Gas Thermometer
Liquid Thermometer
Vent
Gas Thermometer
Manometer
Rotameter
Air Pump
Bath Thermometer
Water Bath
Heater Mag Mix
Electrical Heater
Sewer

Figure 2. Vapor Pressure, p_v, Saturation Concentration, C*, and Henry's Constant, H, vs Gas Temperature, T_{GAS}

(1) $T_{LIQ} = 20°C$



(2) $T_{LIQ} = 30°C$



and 300 rpm, respectively. Torque itself was not recorded but was low due to the low speed and small diameter propeller.

Figure 2 shows the variation of vapor pressure, saturation concentration and Henry's constant with gas temperature at two different liquid temperatures, 20°C and 30°C.

Comparison with Published Data

From Perry's Chemical Engineers' Handbook, the Henry's Law constants expressed in atm/mole fraction are:

T liquid (°C)	H (atm/mole fraction)
20	4.0×10^4
30	4.8×10^4

In order to compare the above values with those reported in Table 1, the units should be converted into mg/l·atm as follows:

For T_{liq} = 20°C

$$H = \frac{1}{(4 \times 10^4 \text{ total kg moles} \cdot \text{atm/kg mole } O_2)} \times \frac{32.0 \times 10^6 \text{ mg } O_2/\text{kg mole } O_2}{(18.02 \text{ kg liquid/total kg mole}) \times (1.0 \text{ l/kg})}$$

$$H = 44.4 \text{ mg } O_2/\text{l} \cdot \text{atm}$$

Similarly, for T_{liq} = 30°C, H = 37.0 mg O_2/l·atm

Table 2 shows a good agreement between the experimental and published values for Henry's constant.

Table 2. Henry's Law Constants

T_{liq} (°C)	Experimental H Value (average) (mg/l·atm)	H Value from Perry (mg/l·atm)
20	44.3	44.4
30	36.9	37.0

Conclusions and Recommendations

1. From the results obtained in this experiment, it is concluded that the value of Henry's constant is independent of the temperature of the gas phase at a given temperature of the liquid phase when the gas phase is saturated with water vapor.
2. The experimental values of Henry's constant are in agreement with the values reported in the literature.
3. It is recommended to perform a series of runs similar to those reported in this study varying the speed of the agitator and varying gas phase vapor pressures.

Section VII. Working Group Summary Reports

27

Group A. Philosophy of the Standard

We are proposing an interim standardized method of testing aeration equipment with respect to ability to transfer oxygen to a reference water. The standardized method should include all details for determination of the performance of the equipment for which there is a consensus by members of the sub-groups. These details should include the mechanics of the tests, sampling, chemical analysis, interpretation of data, and power and air flow measurements.

It is further proposed that this interim standard be included in a Procedural Manual within two years in accordance with the provisions of the EPA grant. The Procedural Manual will reflect the present state of the art.

In implementing the intent of this interim standardized method, the consultant is responsible for providing manufacturers with a volume of aeration basin and a range of oxygen requirements (mg/l·hr) which accounts for alpha, beta, oxygen deficit, etc., when conducting the standard aeration tests in the reference water. Details of any constraints, such as land availability and ground conditions, would also be provided.

The manufacturer will provide power requirements to achieve those specified rates at a given oxygen concentration together with details of capital costs, operation and maintenance costs, noise, flexibility, reliability, tank geometry, etc.

The system will be selected on the basis of cost effective analysis. After selection of the system, bidding is opened to all manufacturers who can meet the specified requirements as determined by the proposed interim standard of testing.

It is expected that the proposed interim standard method will be used for both shop testing and field testing of the aeration equipment.

Group A. Participants

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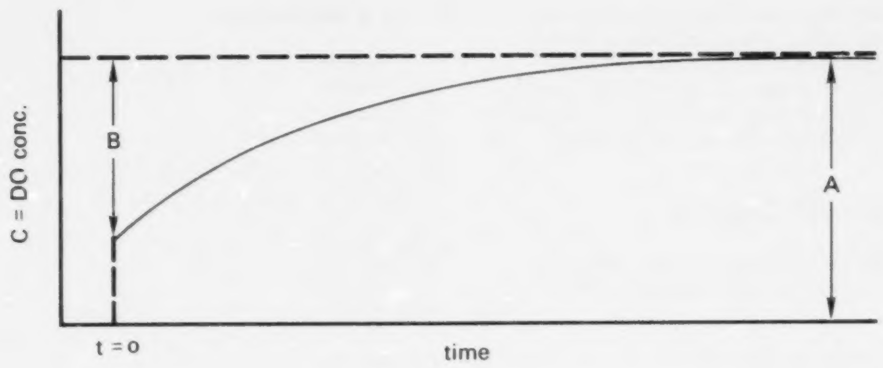
Group B. Modelling and Data Interpretation

The Model

The model for the analysis of non-steady state clean water oxygen transfer data is:

$$C = A - Be^{-(Dt)}$$



Qualifications:

1. A given aerator in a given tank
2. Steady state hydraulic behavior
3. Tank is completely mixed
4. Temperature is approximately constant

This model is similar to the familar integrated form:

$$C = C_{\infty}^{*} - (C_{\infty}^{*} - C)e^{-(K_L a \cdot t)}$$

Group B recommends use of the A, B and D symbols to allow for differences in the way in which these parameters are interpreted for mass transfer models applied to subsurface aeration systems.

Parameter Estimation

The parameters A, B and D should be estimated by fitting the model to concentration-time data by means of a non-linear least squares regression technique. The advantages of the parameter estimation techniques are:

1. It works directly with the observed concentration, C.
2. Truncation of the data as DO concentration approaches saturation is not necessary.
3. For a given set of data, the parameter estimates given by this technique are more precise than those given by other commonly used techniques.

The log deficit method (applied with a measured value of $A = C_{\infty}^{*}$) and the direct method are useful for approximate parameter estimates. However, the differentiation inherent in the direct method produces a variable dC/dt having larger error than the error in C. The larger error in dC/dt causes the error in the parameter estimates from the direct method to be larger than that of the other methods. When the non-linear regression analysis is applied to analyze non-steady state test data, the standard deviation of the parameter estimates should be reported and the error structure should be examined. The error structure may reveal possible surging effects, adequacy of the model and uniformity of experimental error.

Truncation of Data

Values of C less than 10 to 20% of the saturation value, A, may be truncated to avoid lingering effects of deoxygenation techniques and because these data contribute little to the ability to estimate A and D precisely. Truncation of the data as the DO concentration approaches saturation is discouraged because these data significantly influence the parameter estimates for A and D. It is recommended that the test be continued as long as practicable. A period of time approximately equal to six divided by the anticipated value of D is suggested.

It is recognized that temperature variations may affect the precision and accuracy of the test. Therefore, as a minimum, it is suggested that temperature be measured at the beginning and end of the test and that a desirable variation be 2°C or less. The average and extreme values of temperature should be reported. It is recommended that the sensitivity of the A and D estimates with respect to temperature be investigated.

Experimental Design

It is recommended that the analysis be based on a minimum of 10 to 15 data points. Approximately two-thirds of these points should be evenly distributed over the period of time covered by 0.5/D to 2/D. The remaining one-third of the points should be evenly distributed over the period of time covered by 2/D to 6/D. In cases of rapid transfer, the minimum time between data points should be 0.5 min.

When multiple sample points are used, the data from each sample point should be analyzed separately and values of the separate A and D estimates determined and reported for each sample point. The average should also be determined. The uniformity of these estimates may provide useful information about the aeration system.

Interpretation of Parameter Estimates

The physical significance of the parameter estimates depends on the aeration system being tested. For a surface aerator:

$$A = C^*_\infty \, , \; D = K_L a \text{ and } B = C^*_\infty - C$$

For a submerged aeration system, the importance of gas-side oxygen depletion should be recognized in determining a mass transfer coefficient. It is recommended that any manual of practice resulting from Subcommittee efforts include a development of a relationship between D and the volumetric mass transfer coefficient, $K_L a$, similar to that given by the Downing-Boon Model, e.g.:

$$K_L a = D / [1 - (D/\phi_d)]$$

Group B. Participants

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Group C. Non-Steady State Clean Water Testing: Shop and Field

The charge for Group C was to consider what procedures and measurements should be included in a non-steady state clean water testing procedure. The group size varied from 15 to 17 members (see listing of those present). A specific topic was presented and discussed, and a statement representing a viewpoint was drafted and voted upon for inclusion in the Group report. The statements reported below were approved by a majority vote.

Statements 1, 4-7, 11 and 13-31 were approved unanimously. The results of the other voting were as follows:

Statement Number	Yes	No	No Opinion
2	16	1	0
3	13	3	1
8	8	6	3
9	14	2	1
10	10	3	4
12	13	1	0

The discussion of the topics was very thorough and spirited, and the Group should be commended for its drive and effort to reach reasonable agreement on consensus statements.

It is recommended that the testing procedural document include:

1. A requirement that specified conditions for reporting air flow and observed data relating to the existing conditions of measurements be recorded.
2. A statement recommending the use of appropriate codes or manuals for air flow measurement.
3. Appropriate procedures for system pressure measurement exclusive of air flow requirements.
4. A recommended calculation procedure for determining horsepower for air diffusion systems.
5. Appropriate procedures for measuring power for mechanical devices.
6. The requirement of reporting the detailed description of the test geometry for any test conducted.
7. Reference to cleaning the basin and the use of fresh tapwater.
8. A statement regarding the preferred water temperature range for testing.
9. A requirement that an analysis of the physical and chemical properties of the test water be provided to the parties involved in the testing.
10. A specification of initial numerical limits for certain physical and chemical water quality parameters.
11. A statement regarding the conditions for achieving steady state physical conditions prior to test runs.
12. A statement indicating that nitrogen stripping is an acceptable procedure.
13. A statement that the use of analytical grade sodium sulfite for deoxygenation is an acceptable procedure.
14. The recognition that technical grade Na_2SO_3 can be used as a deoxygenating chemical with the provision that adequate quality control to avoid interferences be followed.
15. A statement recommending the use of $CoCl_2 \cdot 6H_2O$ in solution form as the source of cobalt catalyst for deoxygenation.

    Note: The Group altered its editorial approach at this point and has approved the following additional statements.

16. The testing procedural document should address the proper cobalt concentration for testing with due consideration given to adequate catalyst (say >0.10 Co as Co) for deoxygenation and provisions for elimination of DO testing interferences with higher concentrations. A probable maximum concentration of 2.0 mg/l as Co was suggested. (Comment: It was suggested that testing for adequate cobalt catalyst should be required.)
17. The standard procedure should require a sulfite addition adequate to reach zero DO and to remain at zero for a reasonable period (say 2 min).
18. The addition of sulfite in slurry form should be the recommended procedure with an alternate procedure for dry chemical addition delineated, and considerations for multiple point addition should be addressed. (Comment: It was suggested that the sulfite be added in solution form.)
19. The standard procedure should specify a minimum number of sample points (say three) with due consideration to true representation of the tank contents and regions of variable DO levels.
20. The standard procedure should include provision for gravity or submersible pumped samples with the utilization of anti-air entrainment inlets, consideration of pressure changes in gas releases, minimum sample transport times, low bottle displacement times and an adequate number of samples obtained between the test truncation limits.
21. The use of DO probes for in-place and external measurement should be included as an acceptable analytical procedure. It should be duly noted that

appropriate detailed analytical care must be exercised to assure accurate results with probe measurements. (Comment: It was observed that a single sampling and analytical procedure was not recommended and should be considered.)

22. The testing document should address the question of allowable number of sulfite additions per tank of water with consideration given to setting testing limits of sulfate or total dissolved solids concentration.

23. The testing document should provide the option of deleting the initial run on a fresh tank of water to account for uncertain interactions with sulfite addition.

24. The testing document should address the question of chemical interferences and iodine vaporization in Winkler DO analysis.

25. The testing document should address the questions of termination of runs in the case of repetitive testing with consideration of length of run necessary to reach C^*_∞ in every case and obtaining appropriate data for the recommended method of calculation of K_La.

26. The testing document should define a common procedure for attaining a measured oxygen saturation value.

27. The testing document should address the number of repetitive tests required and the reproducibility limits of multiple testing.

28. The testing document should address the question of allowable percent K_La variation in individual sample points and the interpretation of this variation.

29. It is the considered opinion of this Group that the testing document should focus on clean water testing only and surfactants should not be used to modify the clean water test liquid basis.

30. The testing document should consider promulgating appropriate terminology for defining the performance acceptance criteria for different generic types of aerators.

31. Group C questions the use of variations in K_La and bottom velocities in clean water testing as true indicators of mixing capability for solids suspension in aeration tanks.

Group C. Participants

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Group D. Alpha, Beta, and Temperature Corrections

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General

Alpha is affected by wastewater constituents such as soluble BOD, COD, suspended solids concentration, surface tension of the wastewater, and temperature and by system parameters such as type of aeration device, power level, and basin configuration. In general, the influence of these variables can be reduced substantially by conducting the alpha measurement at field design conditions, i.e., testing at waste temperature and using an aeration device and power level similar to the actual field condition.

Beta is influenced by wastewater constituents such as salts, organics, and dissolved gases and by barometric pressure and temperature. Conducting measurements at equal barometric pressure and at field design temperature should reduce variations to those caused by wastewater constituents only.

Theta, the temperature correction factor, is not as well understood as alpha and beta. Several theta factor "constants" have been reported in the literature. The relationship between theta and variables such as aeration device, power level, system geometry and wastewater impurities is not known.

Alpha

A great deal of literature has been published regarding measurement of the alpha factor and the difficulties involved both with the apparatus used and liquids being tested. The type of aeration device, mixing intensity, and wastewater constituents present are only three parameters that affect the outcome of the alpha measurement. Researchers are not in agreement concerning such questions as the effect of suspended solids on alpha, mixing level vs K_La, surfactants effect, and the type of aeration device to be used, to mention a few. The literature contains more controversy regarding these questions than reinforcement.

Of all the variables that potentially influence alpha measurement, these four are by far most significant:

1. Test apparatus and aeration device
2. Turbulence
3. Suspended solids
4. Surface active agents.

Currently there are no standard procedures which take these factors into consideration. As a result, alpha measurement data can be extremely variable and misleading.

Beta

The beta factor has been defined as the ratio of the dissolved oxygen saturation concentration for wastewater at field conditions to the saturation value for clean or tap water at field conditions. Beta has been commonly referred to as the salinity correction factor because dissolved salts reduce oxygen solubility in wastewater. In addition, dissolved organics and gases in the wastewater can reduce oxygen solubility as well. Unfortunately, these constituents also affect the measurement of dissolved oxygen.

Several methods are available for measurement of dissolved oxygen. The various methods can be divided into three principle categories: 1) laboratory methods, 2) electrochemical analysis, and 3) membrane electrode methods. The most popular and most widely used laboratory method is the Winkler Method. However, the amperometric membrane technique is probably the most-used electrode method for measurements in wastewater.

Dissolved organics and dissolved gases can interfere with the rate at which oxygen passes through the electrode membrane and can cause chemical interference in the Winkler Method.

An alternate method of determining beta is to compute the ratio of the saturation concentration for tap water at field temperature, pressure, and salt concentration to the saturation concentration for tap water at field temperature and pressure and zero salt concentration.

When using a probe to measure dissolved oxygen, one must be aware of the sensitivities of the instrument to the waste constituents present in the sample. The probe manufacturer's literature should be referred to prior to calibration and making measurements. If the literature does not contain information on interferences, then the oxygen analyzer manufacturer should be contacted.

Theta

Temperature affects both equilibrium values for oxygen concentration and the rate at which transfer occurs due to changes in kinematic viscosity, which in turn affects oxygen diffusion into the liquid. The effect of temperature on the overall oxygen transfer rate coefficient is usually expressed as follows:

$$(K_La)_T = (K_La)_{20}\,\theta^{(T-20)}$$

The temperature correction factor, theta, has been reported to vary from less than 1.01 to more than 1.05. A comparison of four temperature correction factor references is presented in Figure 1. As can be seen from the curves, temperature correction factors vary by approximately 15% between 10°C and 30°C. Most field conditions are within this temperature range. Caution is advised, however, when correcting field data to standard conditions outside the 10°C to 30°C range where variances become greater. Also, the presence of surface active agents and other waste constituents may affect the temperature correction factor. The potential effects of system variables such as tank geometry, power level, and aeration device are not known. If system variables do influence theta, the magnitude of their influence is probably not significant.

Future Emphasis

The complete lack of concensus regarding the significance and influence of the factors affecting alpha and beta determinations demonstrates the need for a standardized procedure for measurement of alpha, beta and theta factors. Although Standard Methods has a tentative procedure for determination of alpha and beta (207B5b and c), much work remains to be accomplished in the standardization of an alpha and beta factor measurement procedure. Several key factors concerning a measurement procedure are contained in Figure 2. Before detailed procedures can be developed, the major questions such as: 1) What defines equivalent mixing level? 2) What type of aeration device should be used? 3) Should surfactants be used in alpha testing and why? and 4) Are suspended solids in or out, must be addressed and answered? Only after a concensus has been reached regarding these major factors in alpha and beta measurement, can a well defined standard procedure be adopted.

Future emphasis of Group D shall be as follows:

1. Research literature for all known information on alpha, beta, and theta.
2. Define the significant factors influencing alpha, beta, and theta measurements.
3. Recommend research needs to better understand these influences on measurements.
4. Develop a recommended procedure for alpha and beta (and theta, if necessary) measurements.
5. Develop a standardized test apparatus and procedure for alpha measurement.

Group D. Participants

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*Chairman

Figure 1. Effect of Temperatue on Oxygen Transfer Rate, $(K_La)_T = (K_La)_{20}\theta^{(T-20)}$

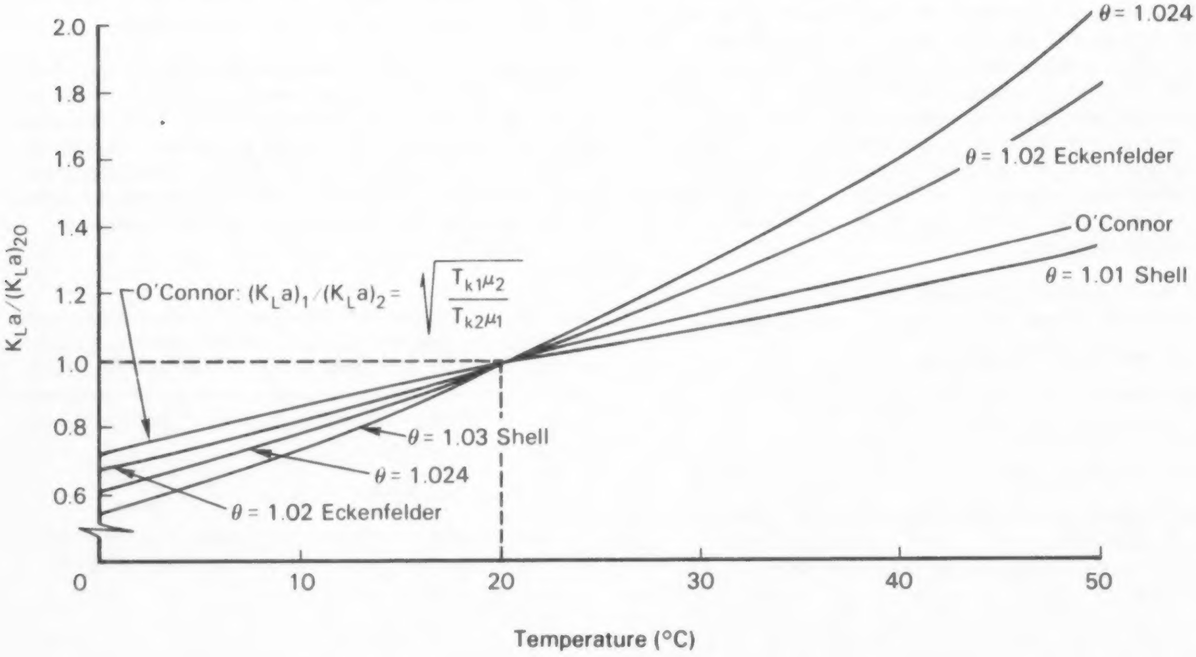
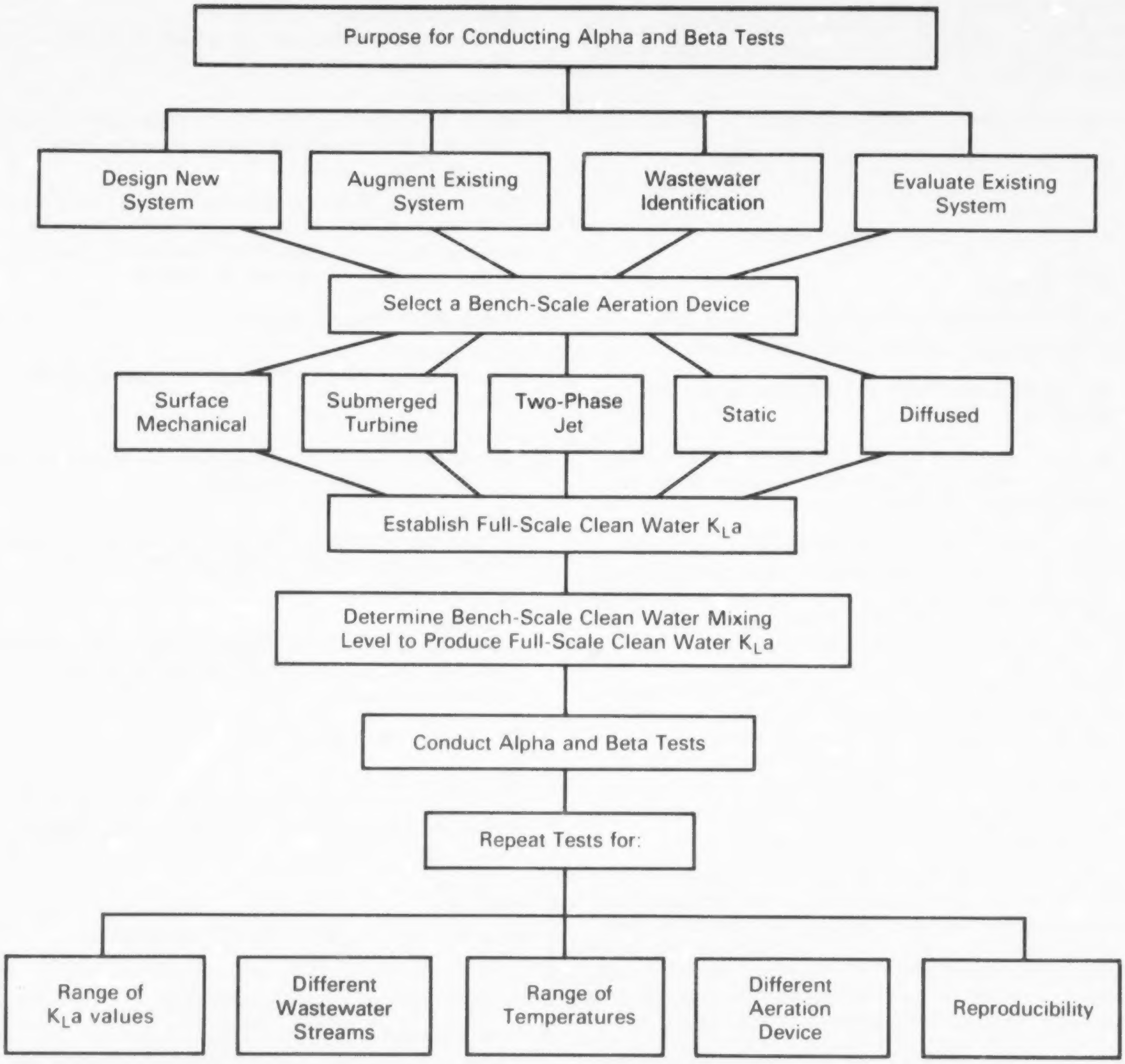


Figure 2. Considerations for Alpha and Beta Measurements


Purpose for Conducting Alpha and Beta Tests
Design New System
Augment Existing System
Wastewater Identification
Evaluate Existing System
Select a Bench-Scale Aeration Device
Surface Mechanical
Submerged Turbine
Two-Phase Jet
Static
Diffused
Establish Full-Scale Clean Water K_La
Determine Bench-Scale Clean Water Mixing Level to Produce Full-Scale Clean Water K_La
Conduct Alpha and Beta Tests
Repeat Tests for:
Range of K_La values
Different Wastewater Streams
Range of Temperatures
Different Aeration Device
Reproducibility

Group E. Steady State Field Evaluations

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The objective of Working Group E was to evaluate the relative merits of steady state and non-steady state methods of oxygen transfer and make recommendations regarding the validity of each method. In order to facilitate the discussion, the subject was divided according to the areas listed below:

1. Types of system in which tests would be conducted:
   a. Shop test system (normally clean water)
   b. Acceptance test system (in shop or field installations)
   c. Evaluation test system (full-scale)
2. Type of test
   a. Non-steady state - batch vs continuous flow
   b. Steady state - batch vs continuous flow
3. Type of equipment (diffused, mechanical, oxygen enriched)
4. Goal of test - must meet specifications expected for *oxygen transfer only* (other specifications would, of course, have to be met).

After considerable discussion, the problem area was divided into two major categories of responsibility: one for the equipment manufacturer, the other, for the design engineer. Specifically the equipment manufacturer should be provided by the design engineer the clean water oxygen transfer rate and associated horsepower for equipment for operation in the final system. The equipment should be tested in clean water (without detergent added) after installation. If the transfer rate is achieved at the specified horsepower, the oxygen transfer requirement will be satisfied by the manufacturer. The test for this in-place, clean water evaluation should be the same as is established by Working Group C.

The decision was made to develop test procedures which would be valid for a wide variety of equipment types and goals. The goals delineated included amount of oxygen supplied each day, horsepower, distribution of dissolved oxygen in space and with time and instantaneous oxygen uptake rate. Mixed liquor solids distribution was felt to be beyond the scope of the oxygen transfer directive.

The tests considered are listed below along with a brief description of the test and possible problems.

1. Steady State Batch Endogenous Test

   What System?
   For continuous flow system for which the flow to the aeration tank can be diverted for the duration of the test.

   Why Batch?
   No wastewater flow during test.

   Why Steady State?
   Parameters evaluated after dissolved oxygen concentration reached maximum

   Description
   Aerate until maximum dissolved oxygen concentration reached after endogenous respiration established.

   Problems
   Defining average dissolved oxygen for high length to width systems. May need curtain (nylon reinforced Hypalon) for lagoon. Saturation dissolved oxygen difficult to obtain on diffused air system.

2. Steady State Continuous Test

   What System?
   For continuous flow system with no flow divergence capability.

   Why Steady State?
   Sample collected from aeration tank being operated as close to steady state as possible.

   Description
   Remove representative sample. Run successive batch oxygen utilization rates to estimate rate at time of sample collection.

   Problems
   Getting good estimate of oxygen uptake rate at time of collection. Load variations. Difficulties with high oxygen uptake rate.

3. Non-Steady State Batch Test

   What System?
   Same as Steady State Batch Test except aeration equipment turned off until dissolved oxygen concentration approaches zero.

   Problems
   Getting organisms back in suspension. Organisms accumulating storage products thus delaying endogenous respiration.

4. Non-Steady State Continuous Flow Test

   What System?
   Same as Non-Steady State Batch Test except open inlet and outlet valve after dissolved oxygen concentration reaches zero.

   Problems
   Same as Non-Steady State Batch Test plus need to keep track of substrate input and utilization.

5. Mass Balance Test

   What System?
   Any continuous flow system.

   Description
   Rate of utilization equal to COD in minus COD out minus oxygen equivalent of cells wasted.

   Problems
   Average daily dissolved oxygen concentration. Close control over solids wasting.

Each of the tests listed above are described in Attachments A through E. Tests which involve tracer methods or off-gas analysis may also be utilized. The use of tracers was discussed by Larry A. Neal in "The Use of Tracers for Evaluation of Oxygen Transfer" presented at this Workshop. Off-gas analysis may be utilized for diffused air, submerged turbine, and closed high-purity oxygen system. Details of this method are discussed by A.G. Boon in "Oxygen Transfer in the Activated-Sludge Process" presented at this Workshop.

Attachment A: Steady State Batch Endogenous Test

Object: To outline the procedure for steady state endogenous respiration testing for evaluating the performance of aeration equipment in activated sludge plants.

Fundamental Concept: Evaluation of aeration equipment in operating activated sludge plants requires careful measurement of the oxygen uptake rate, the alpha factor, the beta saturation value, temperature, and the dissolved oxygen concentration. These data are used in the following equation to determine the K_La value at the operating temperature which can then be corrected to 20°C.

$$K_La = R/\alpha(\beta C^* - C)$$

Because of the difficulty in measuring high oxygen uptake rates, it has been suggested that the endogenous respiration rate of the activated sludge mixed liquor (ML) be used. The endogenous respiration rate generally changes at a rate of only 0.02 hr^{-1} or less so that the oxygen uptake rate will remain relatively constant for a long period of time. The aeration system will allow the the DO to rise to a reasonable level so that the DO can easily be measured.

Procedure:

1. Turn off the influent flow but allow the sludge recycle to continue normally.
2. Adjust the water level in the aeration tank to the level normally anticipated at maximum flow if the aerator performance is desired at maximum conditions.
3. Check the power measurements to insure proper operation of the aeration equipment.
4. Allow the system to aerate for 30 min to 1 hr to use up all excess organic matter.
5. Determine the oxygen uptake rate in the ML, and obtain DO and temperature measurements under the surface aerator at the same time.
6. Collect a sample of ML and allow the solids to settle while determining the oxygen uptake rate.
7. Pour off the settled supernatant and run the alpha and beta saturation tests on the settled supernatant.
8. Determine the oxygen transfer and oxygen saturation values in clean water at the same temperature as the mixed liquor at approximately the same K_La value as in the field system to minimize errors in the results.
9. Repeat the test as desired to insure the validity of the results obtained.

Oxygen Uptake Rate

1. Collect a sample of ML at the edge of the aeration zone so that the sample has a high DO concentration.
2. Determine the change in DO in the ML sample at regular time increments, 0.5-1.0 min.
3. Plot the DO against time on rectangular graph paper and draw the straight line to best fit through the data or use a linear regression analysis for determination of the line of best fit. (Note: If the data do not yield a straight line down to a residual of 0.5-1.0 mg/l DO, the ML may not have oxidized all of the organics and the test should be repeated after an additional 30 min.)
4. Determine the rate of oxygen uptake from the slope of the line of best fit on a mg/l-hr basis.

Alpha and Beta Saturation

1. A laboratory apparatus such as described by McKinney and Stukenberg in "On-Site Evaluation: Steady State vs Non-Steady State Testing" presented at this Workshop can be used for determining alpha and beta saturation values.
2. The apparatus should be tested on clean water to determine the relative K_La for desired test liquid volume, air flow rates, and mixing speed.
3. Once the relative K_La has been determined for the test condition to match the K_La expected in the field, determine the rate of oxygen transfer and oxygen saturation in clean water using the sodium sulfite-cobalt chloride technique or nitrogen stripping for deoxygenation.
4. Once the K_La and the C^* are determined in clean water, the test should be repeated in the ML settled supernatant.
5. The alpha value can be determined as follows:

   $\alpha = K_La$ in ML supernatant$/K_La$ in clean water

6. The beta saturation value in the supernatant can be used directly so that beta does not have to be calculated. If beta is desired, the following equation should be used:

   $\beta = C^*$ in ML supernatant/C^* in clean water

Dissolved Oxygen

1. All DO measurements should be measured with a direct reading DO probe standardized against Winkler DO in clean water.

Attachment B: Steady State Continuous Test

Object: To outline the procedure for steady state continuous testing for evaluating the performance of aeration equipment in activated sludge plants.

Fundamental Concept: Evaluation of aeration equipment in operating activated sludge plants requires careful measurement of all parameters the same as in the steady state batch endogenous test. Occasionally, it will not be possible to stop the waste load and allow the system to come to equilibrium in endogenous respiration. This mode will require a repeat of the same procedures outlined in Attachment A, except that the oxygen uptake rate will be higher.

Procedure:

Oxygen Uptake Rate

1. Oxygen uptake rates must be determined as quickly as possible on ML samples.
2. It will be necessary to add oxygen to the sample and collect the data quickly.
3. Repeat the oxygen addition until the endogenous phase is reached.
4. Use the maximum rate of oxygen uptake for the sample.
5. Project the oxygen uptake rate to zero time when the sample was collected.

Attachment C: Non-Steady State Batch Test

A steady and stable condition is established in the aeration basin under process conditions. Raw sewage and sludge return flows are then discontinued and aeration is ceased. After the dissolved oxygen content drops to zero, aeration is reestablished. Values of C and t are measured at several points within the basin until a stable value of C is obtained. This value is recommended to be at least 3 mg/l but below 5 mg/l for air systems, but may be modified after more experience is gained with the test.

The value of K_La is determined by direct analysis of the data. The slope of the line through the values of dC/dt is K_La. Conversion of the result to standard conditions requires knowledge of the *in situ* values of α and β, the residual C value, as well as temperature and barometric pressure. Transfer effiency may be determined from corresponding power measurements.

Attachment D: Non-Steady State Continuous Flow Test (Kalinske)

This procedure is similar to the non-steady state batch test except that after the DO content of the mixed liquor reaches zero, raw wastewater flow into the basin is reestablished, as is sludge return flow and aeration. This method was reported by A.A. Kalinske in "Problems Encountered in Steady State Field Testing of Aerators and Aeration Systems" presented at this Workshop.

Attachment E: Mass Balance Test

Oxygen transfer is determined by a mass balance over a respiring system. By measuring the biodegradable organic matter entering the system, the biodegradable matter leaving the system, sludge solids wasted from the system by separate sludge wasting and in the effluent and the dissolved oxygen leaving the system in the effluent, an oxygen balance can be made. When all measurements are expressed in oxygen units, the oxygen transferred to the wastewater is equal to the reduction in biodegradable organics minus the active sludge (O_2 equivalent) produced plus the oxygen leaving in the plant effluent. If the raw wastewater contained oxygen as it entered the aeration basin, this quantity must be subtracted from the results above.

Conversion of the results to standard conditions requires determination of α and β values, the residual C value, as well as temperature and barometric temperature. Transfer efficiency may be determined from corresponding power measurements.

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*Co-chairman

Discussion of Working Group Summary Reports

32

The discussion below was transcribed following the presentation of the working group summary reports. Unfortunately, the electronic transcriptions were of poor quality and it was necessary to delete certain discussions which were unintelligible. All discussions have been edited.

Group A. Philosophy of the Standard

Matsch: I would like to bring up a point which concerns me on the philosophy of the development of the standard. How much involvement will the manufacturer have in the selection of the geometry of the aeration basin? If the engineer specifies the geometry in advance, it gives the manufacturer very little opportunity to optimize his equipment in that particular geometry.

Kransnoff: That's a very important point you bring up. The group discussed that at some length during their deliberation. I really don't know, at the present time, whether we should actually put that into a standard. In practice, it would seem appropriate to obtain information on optimal geometry for various devices when a consultant comes up with a design and asks the various manufacturers to comment on whether there is a certain geometry which would optimize their particular equipment.

Fissette: I would like to further amplify on that point. I think what Dr. Matsch says is exactly correct. The way in which this summary statement (Group A) was framed is, the consultant would provide the manufacturer with an oxygen requirement in mg/l·hr and a tank volume. Nothing else would be provided unless there were constraints on land area, soil conditions, or what have you. The consultant would also be responsible for alpha, beta, and theta associated with the wastewater characteristics. The manufacturer would then base his proposal on meeting that oxygen transfer requirement with the best geometry he has. He would optimize his system around the waste characteristics he has been given. Then, the consultant would look at the different alternatives that came in and determine, by value engineering, or least cost or whatever, the best generalized system which he felt would meet his requirements. He would then put them out for bids to the manufacturers. The concept of this philosophy is to have the manufacturer bid his best suit using the geometry which best suits his equipment. Perhaps I am not saying it the way the group felt, but I think that is probably correct.

Hunter: I'm a little confused, but I agree with what everybody is saying here. It seems like we were going to develop a standard for testing, whereas the philosophy seems to be developing a standard for purchasing.

Krasnoff: Actually, the philosophy group was more concerned with the discussion of how such a standard would be used, should it be developed. I think we still have a long way to go before we even have a consensus agreement on a standard method for testing in clean water. But the philosophy is laid out here merely as an indication of how such a standard would be put into use.

Salzman: I guess I was a little confused by the use of the word "interim". I think that in the discussions I have heard here this week, we could come out with the best state-of-the-art recommended test that reflects good current practice but, like any standard, would have to be updated constantly. I think the word "interim" sounds a little too tentative.

Krasnoff: That's exactly what we had in mind. If everyone is bothered by the word "interim", we can cross it out. We did have in mind something which is a current state-of-the-art recommended procedure.

Boyle: I think the word "interim" came about by virtue of the process of review by which a standard would have to go through the ASCE-ANSI procedure. Any recommended standard method would have to go through a period of review and, during that review period, would be called an interim standard. After having been approved by consensus, it would then be called a standard.

Group B. Modelling and Data Interpretation

Shell: How does the non-linear least squares method compare with the log deficit method in determing the parameters?

Baillod: The log deficit method, applied with a measured value of saturation concentration, generally compares well with the non-linear least squares estimates. However, differences are evident and it depends on the actual set of data. For certain data sets, the agreement is very good, for other data sets, not so good. In general, since the data themselves consist of values of C vs t, the group felt that it was preferable to work with the actual C and t data values rather than the use of logarithmic transformations required in the log deficit method. The logarithmic transformation, inherent in the log deficit method, changes the error structure of the data and, for statistical reasons, the most precise estimates of the parameters are produced by handling the raw data themseves as is done in a non-linear least squares fit. The other techniques, such as the log deficit or direct method, are useful approximate methods for data analysis, but the best or most precise method, the group felt, would be the non-linear least squares regression analysis.

Shell: Which parameters were to be measured or estimated from the non-linear least squares technique?

Baillod: All three parameters, A, B, and D. The reason being that if saturation is important, and it is, it's better to determine it from the estimation given by the model fit rather than from one or two measured points near the end of the test.

Mandt: I think your group did an excellent job. The model is really the universal model that we're looking for, recognizing that capital D is not the real K_La for all types of aeration equipment. I see no reason why this model shouldn't be adopted by this Subcommittee.

Paulson: I would like to have you comment on your value of a minimum sampling time of 30 sec.

Baillod: Referring directly to our written report under experimental design, the statement reads, "it is recommended that the analysis be based on a minimum of 10 to 15 data points. Approximately 2/3 of these points should be evenly distributed over the period of time covered by 0.5/D to 2/D. The remaining 1/3 of the points should be distributed evenly over the period of time covered by 2/D to 6/D. In cases of rapid transfer, the minimum time between data points should be 0.5 min.

What we really meant was, using the pipe sample technique and simply saying that in case of rapid transfer where you could not get the minimum of 10 points within that area covered by 0.5/D to 2/D, we would be satisfied with samples taken every 30 sec. If you had probe data, there is no reason why you could not take them at intervals as short as you wished within the response time of the probe.

Saccia: Is C infinity going to be a measured value or text book value?

Baillod: The recommendation is that that parameter will be estimated from the curve fit. We would not be using a "book" value. The recommendation of the committee was not to use a "book" value but to fit the data from the actual test using the non-linear least squares analysis. It was also pointed out by the committee that, if you were going to use the log deficit method for determination of K_La, etc., the actual measured value in the tank should be used and not the "book" value.

Boon: If you are going to use the probe, wouldn't you calibrate the probe against the "book" value?

Baillod: Our committee really didn't get into the question of probe calibration. We felt that was more of a test procedure and would be taken up by a subsequent subgroup. I suspect that one would simply calibrate the probe by a Winkler titration procedure.

Unknown: Could you explain exactly why the committee decided to run the test out to 6/D as opposed to simply running the test out until the dissolved oxygen concentration was relatively constant? Where did the value of 6/D come from?

Baillod: The value of 6/D was kicked around in the Subcommittee for some time and was primarily based, as far as I can see, upon some of the actual experiences of Black and Veatch in the field.

Unknown: But there is no scientific evidence to indicate that 4/D, 5/D, or 6/D would give better information, as far as you know, or give significant improvement in the data? It would seem to be a very arbitrary value.

Baillod: That's true. In my own mind, I can't really see any difference in running at a value of 6/D as compared with running until the dissolved oxygen concentration became relatively constant.

Mueller: During this Workshop there seemed to be some concern about the analysis of data using the non-linear least squares method on the computer. Wouldn't it be viable to incorporate, within the standard, a computer program to analyze the data?

Baillod: I think that probably would be practical. At least a reference could be given to a standard software library.

Shell: I am somewhat reluctant to bring up this point, but I would like to make a couple of comments on the data analysis procedures. I collect a lot of data and have a lot of information which I have to assimilate and, subsequently, use a computer program for this purpose. I have learned that without looking at the data, examining it, or plotting it graphically, mistakes were made that could have been avoided had graphical analysis been performed during the test procedure. I think a major weakness of the proposed approach presented is that there is no graphical depiction of the data.

Baillod: I agree with you. In fact, our recommendation intends that it be done. When we said that the error structure in the data should be examined, we meant that a plot should be made and the data carefully examined. We feel it is imperative that the data be visually observed to interpret the estimated parameters.

Group C. Non-Steady State Clean Water Testing: Shop and Field

Brenner: Two of the major problems which have come up during our Workshop related to clean water testing are: how are you going to collect your samples, and how are: How are you going to collect your samples? and How recommendations still allow a wide variation in sampling procedure and DO measurements. Were there any specific recommendations with regard to either sampling or DO measurements that came out of your group discussions?

Paulson: The group was not opposed to pipe samples vs direct measurement, nor was there any specific objection to the Winkler procedure over the probe procedure for measuring DO. The primary focus was on problems associated with the procedures themselves.

Brenner: Do you think your group will come down to one recommended procedure or another for measuring DO?

Paulson: I think, in the case of looking at the Winkler vs the DO probe, the group would have to go in seclusion for 2 or 3 days to figure out which way to go. I feel that the DO probe should be used, and also that at least one sample point should be run by the Winkler for additional data base. I feel that continuous collection of data, obtained with a probe, is the best way to go. In many cases, the use of a Winkler as a backup verification would be the best approach. Thus, the probe would act as a primary data base source and Winkler would be the secondary base source. As far as sampling is concerned, use of the probe answers that question, also, since you would be using outside probes within the tank. It's possible to use piped samples with outside probes. It limits some problems and creates others. In summary, I believe probes are the most valuable technique to use. Subsequently, Winkler analysis could be used as a backup and as a secondary check on the probe system.

Boon: I would like to open the question about clean water or the use of detergent in clean water. I was particularly concerned with your comment under item 9 in which you state that the physical and chemical properties of the test water should be provided. Have you any specific thoughts on what you're going to have analyzed? Were you going to look at the various surfactants that might be contaminating the water, or confining it to dissolved solids, pH, alkalinity, etc.?

Paulson: We started out to try and define those parameters which should be measured. One of the first suggestions made was that we simply use the drinking water standards. But it was pointed out by some that there were things not listed in the drinking water standards that were surfactants. Someone else had suggested that perhaps we do an alpha and beta analysis on the water in an effort to identify the character of the water. Actually, we did not arrive at any specific listing of constituents that should be measured, nor did we arrive at any kind of decision as to what a standard water should be. This should be addressed in more detail within the coming months.

Boon: I have a few more comments. One is related to a comment you made under item 16. You state that there should be a proper cobalt concentration for testing. I would have thought that it was also necessary to specify the actual cobalt concentration in the test tank at the start of the test run. This would establish that there is still an appropriate concentration of cobalt in the water. I think one of the reasons some find the first test in so-called clean water produces unsatisfactory results is because the cobalt has complexed with impurities within the water. In fact, there is no cobalt concentration during the test run. I think item 16 should be coupled with the specifications of the water itself in order to confirm that the cobalt is in the test water.

Paulson: That's true. When difficulties were found in monitoring oxygen transfer rates during test runs, analyses revealed cobalt concentrations were not present. There are also situtations where difficulties are caused by factors not directly attributable to the presence or absence of cobalt. Our purpose in writing item 16 was to focus on two questions: one, that there be adequate catalyst to catalize the reaction of sulfite and, two, to think about an upper limit of cobalt which may cause some interference problems. That was essentially the extent the problem of cobalt had been discussed in our group.

Shell: When making a water analysis, we always run a complete spectrum of materials within the water, including cobalt concentrations. In the first test of the studies conducted, cobalt concentration has never been a problem. We found the problem (of unsatisfactory test results on first test) is due to an inadequate amount of sulfite. For some reason, the use of 1.5 excess will not completely deaerate the water in the first test. On the second test, it will. On the first test, however, there does not seem to be enough sulfite to deaerate the water and, thereby, we have anomolies. I'm quite sure, however, that it is not a cobalt problem because we have found sufficient cobalt present in the water during the test procedures.

Boon: On item 18, regarding the addition of sulfite in a slurry form, I'm in favor of sulfite being dissolved before it is added to the water. If it is not in solution before the test begins, the undissolved sulfite in the bottom can create difficult problems.

Paulson: I would like to make one comment regarding that. Initially, our group suggested the use of a solution of sulfite under item 18. However, the question was raised as to whether we really wanted to insist on a perfect solution of sulfite. There were experiences where dry feed was working fine, and others where slurry feed was working fine. I think everyone agreed it would be nice to have the sulfite in solution, but maybe we can't afford that in every case.

Boon: Could I go on to suggest if a slurry is to be used in feeding the sulfite to the test tank, a complementary test should be conducted at the start of the reaeration test procedure to insure that no free sulfite is present in the test tank during the reaeration run.

Paulson: That would be an idea. But how are you going to sample these large tanks to get a representative sample during the reareation experiment?

Shell: I will not run a test unless I am sure the sulfite is completely in solution and that it is added immediately above the turbine or the aerator. Regardless of whether the water is warm or cold, it adds a lot of confusion to the test procedure if the sulfite is not completely in solution prior to the test run. I know it is very difficult when you have four, five, or six aerators in the system, but it can be done.

Paulson: I agree. That is essentially the way I normally conduct my test runs. However, I am speaking for the group.

Salzman: The points which this group has tried to cover in the working group summary are more or less a state-of-the-art review. We do not want to be overly restrictive. This is why we didn't say DO probes, as opposed to Winkler, and why we left a little flexibility in the test conditions. We tried to develop a standard people could live with, and then we exercised reasonable control over critical areas. I think that's what the group tried to do rather than set up a perfect test.

Group D. Alpha, Beta, and Temperature Corrections

Brociner: Would it be reasonable to run alpha tests in only large-scale basins so that we can avoid some of the problems associated with scale-up?

Shell: Yes, I suppose that could be done. But one of the problems we have is whether or not there is a sufficient amount of wastewater to test in a particular basin size. Because of the question of scale, we debated among the group members for some time and found a lot of this business on alpha determinations is frustrating. It is misleading to conduct alpha tests in a small vessel with a surface aerator and say that it represents 150 hp, 50 hp, or even a 5 hp aerator. It simply does not.

Brociner: But we do have other small-scale tests we undertake in the laboratory to simulate certain unit processes, such as the centrifuge test or the column settling tests. Perhaps one way to avoid problems associated with scale in submerged aeration would be to increase the depth of the test column.

Shell: It was agreed in simulating submerged aeration, if our wall effect was minimized, we could get a reasonable value of alpha in this particular situation if we used 20 ft, 10 ft, or any depth column.

Scaccia: It is possible to calculate the amount of surface entrainment you get with the small-scale alpha testing process. I'm not sure I agree that in these small-scale units most of the transfer occurs through surface entrainment. In fact, I don't think it does. However, I will agree with you that in these small-scale alpha testing facilities, we do not have geometric and dynamic similarity with full-scale units. My question is the following: If we maintain geometric and dynamic similarity between the small-scale test unit and the full-scale unit, is it reasonable to assume that the alphas will directly scale?

Shell: Well, it depends on the aeration device. I can assure you, after about 5 years of experience, you cannot scale a surface aerator.

Scaccia: I agree that you can't scale a surface aeration unit. You can only scale elements of the unit. You can't take a small-scale unit and say that it would be representative of a large-scale unit. And, if that's what you mean, I would agree with you 100%. But I'm talking about scaling up both the geometric and dynamic properties of these systems.

Shell: I don't think you can make all the laws of scaling comply. We've tried for a number of years without successfully being able to geometrically and dynamically scale these types of units.

Scaccia: I don't agree with that, but let's let that point go. If alpha is nothing but a ratio between the mass transfer in whatever scale you are running in water vs that in mixed liquor, the same thing should happen in a large-scale test. Again, you have a ratio between two numbers. I don't understand why you feel so strongly that alpha cannot be scaled if, in fact, we provide geometric similarity and dynamic similarity in our small- and large-scale systems.

Shell: I think the problem we have is that we're looking at a small-scale unit that is only 12 in. deep. Once you get into pilot-scale, there is probably very little difference between pilot- and full-scale-up of the alpha value. But, notice that most of the alpha-scale data are generated from the Black and Veatch type test device. If one says that represents what is going to happen in the field, this group does not feel that's true. We are saying, let's continue the development to a larger-scale unit alpha testing where we can produce both dynamic and geometric similarities in the pilot- and full-scale unit.

Lestochi: If we start with the premise that an alpha in small-scale is different than an alpha in full-scale, is the alpha in the small-scale system smaller or larger than in the large-scale, or do we know?

Shell: We do not know.

Lestochi: All right. Then, my comment is that I think we are wasting our time on trying to develop a standard to test aerators. If we're going to test aerators to find out what their oxygen transfer capabilities are when properly applied in the field, we need to know the value of alpha in order to apply them in the field. But, if we don't know alpha values within ±50 or 60%, then we're going to be off that much in trying to apply this aeration equipment in the field. If we can't put a handle on the very fundamental problem, like defining alpha, why are we trying to be exact in identifying the testing procedures for oxygen transfer devices?

Shell: We've seen from Gary Gilbert's presentation the error effect of the estimation of both alpha and beta, and we recognize that it is enormous. What the consulting engineer needs is some handle on the application of the aeration devices to the field and we have to start somewhere. We're not using a micrometer, but we're saying that we want to develop a test procedure that indicates what the surfactant effect is going to be on that aeration equipment. Now, if the only sample I have is raw wastewater, then I'll have to run alpha on raw wastewater. If all we have is bench-scale facilities, I'll have to run it on a bench scale. It may be wrong, but it will be something of a guide. What we want to emphasize is not to stop there. Try to get a correlation between the bench- and full-scale.

Lestochi: But at some point I need to know what that number is before I can scale to the field.

Shell: That's true, but in the opinion of the group that has met here, we don't have that capability. We don't have a procedure that is going to tell you precisely the value that will occur in the field due to the scaling effects, the geometry effects, the surface effects, and etc.

Scaccia: First of all, we have to distinguish between two things, that which is available in the literature and what is actually known from independent tests. I personally feel that there isn't much difference between the alpha run at small-scale and large-scale. It's not a fact that we don't know anything about the test or that we are wasting our time with this measurement.

Ernest: It seems this whole alpha factor is actually based on the wastewater and it varies from hour-to-hour and day-to-day. Even when we test a sample, we are going to be subjected to a great deal of variation depending upon the day or the hour which we took the sample for analysis. This is something we will have to accept. We need an oxygen transfer device which is highly flexible and will be able to transfer oxygen under a wide variety of situations from hour-to-hour and day-to-day.

Boon: I thought the philosophy group (Group A) very neatly got around the problem associated with alpha in the development of standards by throwing the ball back to the people who knew or should know the most about the characteristics of the wastewater, the consultant and the user. The philosophy group had suggested that aeration equipment manufacturers would be given a required transfer rate and the process volume. Thus, it doesn't matter whether the water is clean or dirty provided that the standard is universally adopted. I think it is extremely difficult to have a standard for a clean water, because clean water varies just as much as sewage varies from region to region and supplier to supplier. If you adopt a standard for clean water, you are going to be in a situation where you have people contesting that the tests carried out in the shop with so-called clean water will give one result when it is tested in the field, and another when tested in the shop. And, you will be forever trying to get the full-scale test facility clean water of the same quality as that in the shop test facility. As to the question of alpha, this is something for the consultant to decide. What I have heard today is that it is a procedure which the consultant should be responsible for. The consultant must be sure that his specifications for the aeration equipment allows for the variability of alpha in the waste. And as Ernest has quite rightly said, a range of alphas must be specified to the manufacturers in order to provide sufficient flexibility to allow for the variability that will occur in the plant. I don't know how you build your plants in the U.S. but in the U.K., when a plant is finished, it is designed for 5 to 10 yr hence. That is your flexibilty to a certain extent. The flexibility is also there in the aeration devices which are specified by the engineers. Sufficient flexibility must be built in the plant to provide for the day-to-day and hour-to-hour variations which are normally expected in any type of plant.

Boyle: I would now like to focus the questions on this group's (Group D) comments on beta and theta determinations.

Brenner: Why didn't you prescribe the use of the Winkler procedure for DO concentration for beta measurements?

Shell: Because there are so many interferences in wastewater that make it very difficult to use the Winkler procedure for accurate measurement of DO. The Winkler is not a viable method for most wastewaters, particularly, industrial wastewaters.

Unknown: Why were no specific recommendations made for a value for theta by the group.

Shell: We really do not have sufficient information on the effect of mixing intensity, hydraulic regime, and etc., on the temperature coefficient. As yet, it has not been well defined and there has not been a large amount of work done on the temperature coefficient for gas transfer. It was not the intent of the group to avoid recommending a table of temperature coefficients, for example, for submerged turbines, surface aerators, or diffused aerators. This can be done. We must have some kind of factor to use in field application. The group did not feel that a testing procedure for theta was appropriate at this time. This procedure may be developed over the years, but there's still a lot of research that needs to be done.

Mueller: I think it should be made clear that it is the charge of this Subcommittee to put together the best information that's available in the field. Five years hence, we will evaluate that information and improve on it. What Shell is saying is we do not have very much information on theta, but we do have some. We should begin to collect information on the influence of temperature on oxygen transfer for various generic devices. Hopefully, the manufacturers will be collecting that kind of information and they will be making it available to the profession. It could be a significant contribution as far as this particular problem is concerned.

Group E. Steady State Field Evaluations

Scaccia: In the procedure which you outline for the non-steady state test as discussed yesterday by Kalinske, there is concern related to what would happen in the system when the aeration equipment is shut down for deaeration. It would seem that there would be tremendous effects on the mixing and turbulence regimes within the basin when the aeration system is started back up and oxygen transfer is subsequently measured. There will certainly be mass transfer problems. For example, the gas transfer through the floc itself could be affected by such a non-steady state hydraulic regime. Are you convinced that this type of technique could be applied for field evaluation?

Stukenberg: I'm not so sure I'd want to say it can or cannot be applied. In our discussions, we felt that the use of submerged turbines or a jet aeration device might produce enough pumping during the deaeration period. These might be better suited to this test than a surface aeration or diffused air system. I agree with your concern, and we discussed this at some length. We are changing a lot of things within the basin, the dimensions of the floc, the mixing characteristics, etc. There are certainly a lot of things that could be going on in the basin that would make one wonder about the test. We looked at all the tests that are currently available for field testing, and, based on the comments which you have made and our own feelings, we did not feel that this would be an acceptable evaluation test procedure.

Mueller: When you are running this continuous steady state test with the high uptake rate, your procedure in reoxygenation of the sample seems futile since you've already lost the initial organics. As far as I can see, you're beginning to work in the endogenous phase, which is not related to your initial uptake.

Stukenberg: You're right. What we are saying is, if the oxygen uptake rate is high enough and the DO is depleted before you get into endogenous, we will continue to add data to the curve. We are actually trying to get back to what the uptake was.

Mueller: Dr. Matsch had mentioned in our working group discussions the possibility of getting the uptake rate very rapidly with some new equipment that's arriving on the market.

Paulson: Prompted by your comment (Group E Summary), whereby you state that the equipment should be tested in clean water and definitely specify *without detergent added,* raises the rather controversial point regarding the characteristics of a "clean water". I think before this Workshop breaks up today, it would be wise to briefly discuss some of the pros and cons associated with the addition of detergent to water as a standard fluid or standard water.

In the deliberations of Working Group C on a non-steady state clean water test, the point was raised whether or not a detergent should be added as a part of the clean water test. Several questions were raised: How do we conduct the test? (That is, what are the problems with measuring the surfactant concentrations?) What type of surfactants should be added? And what are the problems of conducting the test itself regardless of any other chemistry involved in the water? Another question was: How are we improving the situation of the standard test by adding a surfactant to the water?

The purpose is, of course, to give us a better answer as far as the performance of the equipment in the field. But, how does the addition of the detergent get away from the same problems that we had to start with, the variable water chemistry? It would seem that we would be adding a further complication to the test procedure. I would like to have the comments of the individuals in this Workshop.

Boon: I'd like to see tests run both in the "clean water" and in clean water with detergent added. That is the type of system which we've adopted in the U.K. for testing of aerators. I am sure that by adding detergents, one will add complications, particularly with the Winkler determinations. If one carries out tests with detergent addition, one will have to do them with DO probes since it is far more difficult to get an end point with a titration when detergents are added than with clean water.

The big problem though is, "What is clean water?" Contaminants in "clean water" will have a substantial effect on the oxygen transfer coefficient, particularly with fine bubble diffused air systems. Surface aeration systems are less affected by the presence of contaminants in the water within ±10%. With the fine pore diffused air systems, the effects of the contaminants could be ±20 or 30%. The addition of detergents overwhelms all these small effects caused by contaminants. Thus, the value obtained should not be affected by the initial contaminants that may be present. There isn't an accumulative effect due to the addition of more surfactant. It is the total effect of the 5 mg/l of the detergent which swamps the effect of the fractional concentrations of other surface active agents that might be present in the water, or the trace contaminant derived from the unclean aeration basin.

The big problem is making the comparison between the shop and field testing. The shop test basin, which is normally small enough, can be cleaned, and it is possible that the water used in these tests is quite clean. In my experience, that is possible. But, in field testing, the situation is much more complicated. What I'm concerned about from your point of view is putting this down in a standard. The aim is to get the manufacturer to test his equipment in a given tank geometry and in the reference fluid, which you decide, thereby specifying the amount of power to transfer oxygen at a given rate into the test water. When the equipment manufacturer wins the contract, the equipment is then tested under field conditions to evaluate whether the equipment actually works. It is probable that under field conditions, different results will be obtained from the shop conditions. In the past, this could be attributed to the geometry of the tank. But, hopefully, based on the acceptance of the recommendations made by Working Group A, that can be avoided. Then the question of the nature of the test water comes into the picture. If the results are lower in the field test tank than in the shop test tank, the excuse may be that the water isn't clean. I can imagine arguments coming up by the manufacturers claiming that the water tested in the field was not the same as that tested under shop conditions.

Shell: In reference to your comments, Mr. Boon, with special emphasis on the term reference liquid in the U.S., it is the responsibility of the contractor to run the tests. He transfers that responsibility back to the equipment supplier. The contractor and the equipment supplier must then clean the tank and prepare for the test and make sure the reference water being used is what they choose. They certainly can't come back later and say it isn't the right water, and I can't think of a point in my career when this particular point was made. It is their responsibility. If they want to use river water, fine. If they want to use effluent, fine. But, they're going to have to live with those results. We have always used tap water, as clean as available, for on-site testing. It might be river water in order to get enough to do the testing. Again, I want to emphasize that I don't really see any problem in this area, as far as a reference fluid, because it is the contractor's and equipment supplier's responsibility. It's written in the specifications that clean water shall be used. If they use dirty water and don't pass the test, it's their own problem.

Brociner: We already have alpha, beta, and theta, and now we are introducing still another complication in our test procedure with the addition of surfactant. I feel that we are over-complicating our whole standard procedure by this addition. I don't see any value in adding surfactants to clean water tests.

Brenner: Although there's been a good deal of discussion about the use of a standard reference water in developing a standard, I think the real issue here is primarily one of not being able to adequately predict the full-scale capacity of aeration equipment. It's very hard to go from clean water shop tests to dirty water field applications. We might have wastewater alphas ranging from 0.4 to 0.8, with a wide variation from hour-to-hour and day-to-day. Admittedly, it's quite easy to use clean water test data and then scale-up with some alpha value. But, if the addition of the detergent will give us a handle on what might be expected in the actual field installation, we might be better off in the long run with standard tests which use the detergent. Mr. Boon has indicated that, with a 5 mg/l detergent addition, the value of alpha obtained is quite similar to the actual field alpha that one would observe with municipal wastes containing only small amounts of industrial wastes. Therefore, we would not be getting equipment that's undersized. If this is true, and I am not able to say whether it is or not, since the use of the 5 mg/l detergent simulates wastewater from municipalities, why don't we think about the addition of detergent to clean water as our reference fluid? It would put us ahead in the Step 1 design phase since it would give us equipment that would be sized more realistically for the wastewater. It would be an advantage to be able to better design equipment earlier in the design phase. I think it's a good idea to look into the concept of adding detergents to standard test water, and I hope that the Subcommittee will at least consider it in their deliberations.

Boon: I am sorry I didn't elaborate on what the addition of 5 mg/l of detergent to clean water would do in relation to the actual transfer of oxygen in wastewater. Brenner made the point that the addition of the detergent makes the clean water behave more nearly like municipal wastewater. However, I want to make it clear that you would still have an alpha. In the work done, we've actually defined alpha prime (alpha') as the relation between the K_La in the wastewater and the K_La in the clean water with detergent. We found that the variability of alpha' is not anything like the variability in alpha (which, of course, is defined as the K_La in the wastewater to the K_La in clean water). In a given system, alpha varied from a value of about 0.3 to 1.0, whereas, alpha' varied from about 0.8 to 1.2. That gives you a feeling as to why we in the U.K. have decided to standardize on a 5 mg/l surfactant addition to the clean water test system. Now, if you are using a surface aeration system, the variation in alpha from clean water to mixed liquor is not as great as compared to the fine bubble diffusion system mentioned earlier. Normally, the alpha value for the fine bubble diffusers systems are substantially lower than for surface aeration equipment. (I want to stress that the value of alpha does not relate to the efficiency of the gas transfer device.) Therefore, it is possible to get a grossly underdesigned system if clean water test data are used for fine bubble aeration systems and extrapolated to field conditions with an alpha which is considered to be typical for aeration devices. (Normally, alpha values for fine bubble diffusers may range around 0.3, 0.4, or 0.5, whereas, values for surface air entrainment devices may range from as low as 0.85 to higher than 1.2.) Of course, if you have measured alpha under the actual test conditions you expect to have in the field, there should be no problems with fine bubble systems, but, if you are estimating it, you could make an error in order of magnitude of 1.0, 2.0, or 3.0 on the low side.

Fullerton: The emphasis of the Workshop was based on measurements of oxygen transfer for aeration systems. There was very little discussion relative to oxygen transfer for pure oxygen systems. This emphasis was correct. However, it is a concern that if standards are promulgated without explicit definition of oxygen systems as an exception, the same principles may be applied to all systems. All this time, FMC Corporation cannot identify the possible impact of adhering to standards imposed for aeration devices as applicable to the open tank pure oxygen process. But, FMC Corporation is secure in the present methods for determination of oxygen transfer efficiency using the off-gas method during a steady state operational mode. This procedure is certainly indicative of the actual system operational efficiency and has, to date, been accepted by the engineers and owners as an acceptable method for performance evaluation. The primary exception to the aeration test is the clean water performance. Currently, we do not have enough data to identify existing problems with the test as a required method. Until such time as FMC Corporation can evaluate the impact of adhering to a suggested manual of procedure (standard), we would prefer to remain as an exception with a definition of *in situ* off-gas testing as an acceptable alternative.

Mueller: I don't know enough about the FMC procedure to ascertain its accuracy. In my paper on closed systems, I indicated that off-gas analysis was the preferred methodology since inaccuracies in O_2 uptake measurement were bypassed. However, the technique does require an accurate determination of off-gas flow rate for each stage of the covered reactor. The same requirement applies for an open tank reactor. I don't know FMC's technique to get gas flow, but accurate determination in an open system may be difficult.

In the closed tank aeration system, the same aeration principles apply whether air or pure oxygen is utilized as long as the gas phase mass balances are used to provide O_2 gas phase partial pressures. In the open tank system, the change in O_2 parital pressure and gas flow with depth will probably be required. If done accurately, I would think the same principles apply for air and oxygen systems. However, if not done, then pilot- or full-scale data *in situ* would be required. This situation also exists with the closed tank system if a model calculating gas phase partial pressure is not utilized.

Appendices

Appendix A
Announced Workshop Program

Tuesday, April 11, 1978

1:00 p.m. **Registration**

2:00 p.m. **Opening Remarks**
William C. Boyle, Prof.
University of Wisconsin
Madison WI

A Perspective on Oxygen Transfer

Session Chairman:
W. C. Boyle

2:05 p.m. **Oxygen Transfer: A Historical Perspective — The Need for a Standard**
Wesley W. Eckenfelder, Prof.
Vanderbilt University
Nashville TN

2:35 p.m. **Philosophy of and Perspectives on a Standard — A Panel Discussion**

Manufacturer:
George Fissette
Ralph B. Carter Co.
Hackensack NJ

Consulting Engineer:
Edwin Barnhart
Hydroscience, Inc.
Westwood NJ

Owner:
Lawrence Ernest
Milwaukee Sewerage Commission
Milwaukee WI

ASCE:
Robert Crist
Publications and Technical Affairs
New York NY

U.S. EPA:
Richard Brenner
Municipal Environmental Research Laboratory
Cincinnati OH

3:30 p.m. **Break**

State-of-the-Art Review

Session Chairman:
W.C. Boyle

3:45 p.m. **Review of Model Refinements and Data Interpertation**
C.R. Baillod, Prof.
Michigan Technological University
Houghton MI

4:05 p.m. **Oxygen Transfer Parameter Estimation**
L.C. Brown, Prof.
Tufts University
Boston MA

4:25 p.m. **Discussion**

5:30 p.m. **Adjourn**

Social Hour and Dinner

7:30 p.m. **Review of Test Procedures**
Wayne Paulson, Prof.
University of Iowa
Iowa City IO

Clean Water Testing: Shop and Field

Session Chairman:
Charles Matsch
Union Carbide Corp., Linde Div.
Tonawanda NY

8:10 p.m. **Influence of Tank Geometry**
Thomas Rooney
Rexnord Corp.
Milwaukee WI

8:50 p.m. **Influence of Mixing**
Ronald Salzman
Mixing Equipment Co.
Rochester NY

9:30 p.m. **Sampling Considerations**
Gerry L. Shell
Gerry Shell Environmental Engineers
Brentwood TN

10:10 p.m. **Dissolved Oxygen Measurement and Saturation Values**
V.T. Stack, Jr.
Betz, Converse & Murdoch, Inc.
Plymouth Meeting PA

10:50 p.m. **Adjourn**

Wednesday, April 12, 1978

7:30 a.m. **Breakfast**

Clean Water Testing: Shop and Field (Cont'd.)

Session Chairman:
Robert Irvine, Prof.
Notre Dame University
South Bend IN

8:15 a.m. **Influence of Temperature**
John S. Hunter, III
3M Company
St. Paul MN

8:55 a.m. **Influence of Water Chemistry**
Donald Burns
Eimco PMD, Envirotech Corp.
Salt Lake City UT

9:35 a.m. **Air Flow and Power Measurement**
Fred Yunt
County Sanitation Districts of Los Angeles County
Carson CA

10:15 a.m. **Break**

10:30 a.m. **Data Interpretation: Non-Steady State Clean Water Tests**
Lloyd Ewing
Ewing Engineering Co.
Milwaukee WI

Jerry Wren
Water Pollution Control Corp.
Milwaukee WI

Mikkel Mandt
Pentech Div.
Houdaille Industries, Inc.
Cedar Falls IO

11:20 a.m. **Measurement of Alpha and Beta**
Gary Gilbert
Kenics Corp.
North Andover MA

12:00 **Lunch**

1:00 to 6:00 p.m. — **Free**

6:00 p.m. **Dinner**

Clean Water Testing: Shop and Field (Cont'd.)

Session Chairman:
Robert Irvine

7:30 p.m. **Comparison of Shop and Field Tests**
V. Wahbeh and John R. Stukenberg
Black and Veatch
Kansas City MO

Evaluation of Respiring Systems

Session Chairman:
Haskel Brociner
FMC Corp.
Itasca IL

8:10 p.m. **Oxygen Transfer in Closed Systems**
J.A. Mueller, Prof.
Manhattan College
Bronx NY

8:50 p.m. **On-Site Evaluation: Steady State vs Non-Steady State Testing**
John Stukenberg
Black and Veatch
Kansas City MO

Ross E. McKinney, Prof.
University of Kansas
Lawrence KA

9:30 a.m. **Problems Encountered in Steady State Field Testing**
A. A. Kalinske
Camp, Dresser and McKee
Walnut Creek CA

Tracer Methods

Session Chairman:
Haskel Brociner

10:10 a.m. **Use of Tracers for Evaluation of Oxygen Transfer**
Lawrence A. Neal
Law Engineering Testing Co.
Marietta GA

10:50 p.m. **Adjourn**

Thursday, April 13, 1978

7:30 a.m. **Breakfast**

8:30 a.m. to 12:00 **Meeting of Working Groups A-E**

12:00 **Lunch**

1:30 to 5:00 p.m. **Meeting of Working Groups A-E**

5:30 p.m. **Social Hour and Dinner**

Evening is Free

Friday, April 14, 1978

7:30 a.m. **Breakfast**

Presentation of Working Group Summaries

Session Chairman:
W. C. Boyle

8:15 a.m. **Group A — Philosophy of the Standard**

8:45 a.m. **Group B — Modelling and Data Interpretation**

9:15 a.m. **Group C — Clean Water Testing: Shop and Field**

9:45 a.m. **Group D — Alpha, Beta, and Temperature Corrections**

10:15 a.m. **Group E — Steady State Field Evaluations**

11:00 a.m. **Adjourn**

Appendix B
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Appendix C

Procedures Governing American Society of Civil Engineers Development of Standards

1. General. The procedures described herein refer to the adoption of new standards, revision of standards, reaffirmation of standards, and withdrawal of standards, all of which are referred to as standardization activities.

   The ASCE Board of Direction delegates the authority for the development of standards and associated activities to the Technical Council on Codes and Standards (TCCS) and to its Executive Committee (TCCS/EC).

2. The TCCS/EC shall furnish guidance to the Board of Direction on policy and other matters relating to codes and standards. TCCS/EC shall also establish liaison with appropriate Technical Divisions and other Technical Councils of the Society to insure technical input from such groups when the work of such Technical Divisions and Councils relates to a specific standard activity under the jurisdiction of TCCS.

3. TCCS/EC will establish liaison with appropriate government agencies, industry, and other organizations for guidance in standards activities of mutual concern. It will have the delegated power to authorize and supervise all standards development programs undertaken for, or in cooperation with, other organizations. This will include participation in standards writing activities of such other organizations.

4. When the need for a standardization activity arises, TCCS/EC shall consider a proposed standardization action and, if appropriate, establish a preliminary set of objectives, organize a standards writing committee, and request approval of Management Group A (MGA) for the establishment of either a task committee or a committee for the specific project. It shall notify the appropriate Technical Divisions and Councils of such action and publish a notice in the ASCE News.

5. When continued or permanent maintenance of a standard is required, the TCCS/EC will recommend to MGA that such group has the status of a continuing standard writing committee. If no such maintenance is required, the group shall have the designation of a task committee.

6. All actions of TCCS standards writing committees shall be monitored by TCCS/EC, including make up of the membership, expenditures of funds for travel reimbursement for control group members, engagement of consultants, etc., as provided for in an approved budget and under established procedures. TCCS standards writing committees may, if approved by TCCS/EC and with concurrence from the Executive Director, seek outside funds for a standards activity.

7. TCCS standards writing committee membership shall be made available to thoseinterests substantially concerned with the scope of a proposed standard and shall be balanced among those interests and selected from ASCE members with competence in the field of of the proposed standard except as noted in item 8 below. The executive committees of all concerned Technical Divisions and Councils may have a representative on each standard writing committee at their option.

8. Each TCCS standards writing committee shall organize itself and subcommittees or subgroups with appropriate members and officers (Chairman, Vice-Chairman, etc.), budget, scope, schedules, and procedures for approval. TCCS/EC officers and control groups will be monitored by and appointed yearly by the TCCS/EC. Individuals may be reappointed and hold office for a maximum period of 4 years.

9. Non-members of the American Society of Civil Engineers may serve on a TCCS standards writing committee as advisory members but shall be limited to voting on matters at the standards writing committee level. TCCS/EC will approve non-members to standards writing committes when the standard writing activity requires special expertise or broad representation to create balanced interest in that activity.

10. All TCCS standards writing committees shall give consideration to:

    a. The existence of other comparable standards having national acceptance in a given field.

    b. The potential use of a given standard.

    c. The technical quality and use of established, broadly accepted engineering practices.

d. The results of patent rights or other factors which might favor a product for an industry.

e. Minority or dissenting views of members of standards writing committees. Such views, if not resolved, must be reported in detail to TCCS/EC.

11. A simple majority of a subgroup and standards writing committee is necessary for actions at the operating level of such groups.

12. Every effort shall be made by a TCCS standards writing committee to obtain a unanimous vote of its members on a proposed standard. If a unanimous vote cannot be obtained, the proposed standard shall have the approval of at least three-fourths of the total standards writing committee members. Each negative vote shall be accompanied by a detailed reason for such vote and shall be circulated to all committee members. Every effort should be made to resolve negative votes. The responses to each dissenting comment by the standard writing committee shall be sent to the proponent. All dissenting votes, if not resolved, must be reported in detail to TCCS/EC.

13. After approval of a proposed standard by a TCCS standards writing committee and transmittal, along with detailed information on negative votes, to TCCS/EC, the proposed standard and the voting shall be reviewed by TCCS/EC. Consideration shall be given to negative votes and procedures. If deemed necessary, the proposed standard shall be sent back to TCCS's standards writing committee for clarification.

14. The TCCS/EC upon approval of a proposed standard shall direct the ASCE staff to take the necessary action to publish a notice to the full membership of the TCCS. Council members will be given an opportunity to vote on all standards and this will be designated as the Council Ballot. Cut-off dates for such balloting shall be 6 weeks from the notice date (exact procedures for communicating with the Council not yet established).

    a. Council Ballots shall be canvassed by TCCS/EC as early as possible after their receipt at ASCE Headquarters.

    b. All negative votes, to be valid, shall be accompanied by a written explanation based upon either technical or procedural considerations and must be directly related to the proposed standard.

    c. Negative votes shall be sent back to the appropriate TCCS standards writing committee for consideration and resolution. The standards writing committee shall report back to TCCS/EC the detailed consideration and resolution of all negative ballots. TCCS/EC shall determine that the standard was developed in conformance with these procedures and that due process was given to all negatives.

    d. A proposed standard must receive approval by two-thirds of those casting valid ballots.

15. Upon final approval of a proposed standard by TCCS, it may be submitted to the American National Standards Institute (ANSI) for approval as an American Standard.

16. The TCCS/EC shall establish procedures to insure that ANSI criteria for accreditation are maintained on a continuing basis.

17. Copies of all reports (special reports, minutes, etc.) of of TCCS standard writing committees shall be submitted to TCCS/EC and ASCE Headquarters.